Manual Switches for Aircraft

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Synopsis: In the design and application of electrical control equipment the problem of obtaining complete field data and requirements is always present in some degree.

The problem is acute in branches of the electrical industry supplying aircraft control. Three factors contribute to the difficulty:

1. The tremendous engineering effort centered in the aircraft industry causes rapid technical changes that soon make accumulated data obsolete.
2. Technical data on utilization devices are not always available to either the aircraft or electrical manufacturer. The mushroom growth and technical advance of the aircraft industry have, in some cases, outdistanced laboratory equipment and personnel capacity so the data an electrical-control manufacturer would like to have are simply not available.
3. The secrecy surrounding many aircraft developments blocks the passing of information from the aircraft manufacturer to the electrical manufacturer.

The electrical-control manufacturer can meet this problem, in part, by making available complete performance data on switching equipment offered for sale. Properly chosen, these data enable the aircraft engineer to match control means with his electrical utilization device requirements.

In the preparation of such data the following considerations are of primary importance to both the user and producer of switches.

The functions of an aircraft switch may be classed broadly as follows:

1. To establish a circuit.
2. To maintain a circuit.
3. To interrupt a circuit.

Performance of these functions is importantly affected by the following characteristics of the load:

(*a*). Inrushes.
(*b*). Inductance.
(*c*). Current.
(*d*). Voltage.

IN establishing a circuit, the increasing order of difficulty shows:

(*a*). Inductive loads.
(*b*). Resistive loads.
(*c*). Lamp loads.

When a set of switch contacts closes, the circuit is seldom established without some disturbance (see Figure 1). The current magnitude at the time of this disturbance becomes a major factor in switch performance. Contact erosion results, caused by the arcing which normally accompanies such disturbance, and in extreme cases freezing or welding occurs with the possibility that the switch may immediately become inoperative through inability to open the freeze. In most switches the contact bounce that creates these disturbances ends within ten milliseconds, and the current magnitude for that period of time is of primary importance in connection with this first function of a switch, establishing the circuit.

(*a*). Inductive loads build up slowly, and the current for a time after the switch contacts close is less than steady-state value. For this reason damage to contacts is minimized.

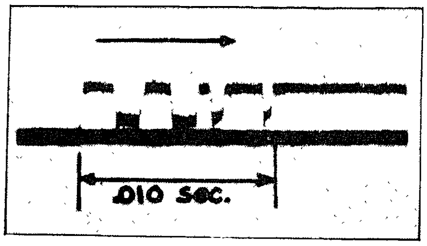

Figure 1. Switch contact bounce

(*b*). Resistive loads cause more damage in establishing the circuit than inductive loads do because of the substantially vertical front current increase. This means that any bouncing of contacts (with accompanying arcing) occurs at practically full steady-state circuit load instead of the fraction of steady-state current indicated in the preceding inductive-load discussion.

(*c*). Inrush loads are most severe in circuit establishment. The common forms experienced are lamp and motor inrushes. The former—because of their characteristic vertical front current increase, and because of the relatively slow decay of the inrush—are the most severe of any circuit-closing condition. Any switch bouncing is almost sure to occur at currents well above steady-state values. Motor inrushes are not comparably severe because the inductive component tips and rounds off the wave front in a manner so bouncing usually occurs before maximum inrush current is reached. Condensers have a steep wave front like resistance, but the circuit ordinarily has other limiting resistance.

The second function of the switch, maintaining the circuit, is mainly affected by the circuit characteristics of current and voltage. The passage of current across the internal resistance of a switch results in a wattage loss that must be dissipated without causing harmful temperature rises in either conducting or insulating members. Experience indicates that, if the continuous duty rating of a switch be taken as 100, the 15-minute, 5-minute, 1-minute, and 5-second ratings may be taken as 110, 150, 200, and 400, respectively, for the average switch used in aircraft circuits. Losses must also be limited to the extent necessary that excess voltage drop will not obtain. The circuit voltage (which may vary from supply voltage) strains the switch insulation, which must, under all operating conditions, be capable of restricting current flow to switch parts that are normally current-carrying members. Conducting paths to ground or opposite polarity result from voltage breakdowns, and such paths may carbonize and progressively develop more wattage until complete failure occurs. The stripping of conductor insulation at the switch requires the provision of adequate circuit insulation from terminal to terminal through the switch.

The third function of the switch, interrupting the circuit, is affected by circuit current, voltage, and inductance. Figure 2 shows a switch opening a resistance load of 30*A* at 30*V* and the same switch opening a highly inductive load of 25*A* at 30*V*. The arcing time for the inductive but slightly smaller load is more than 12 times that for the resistive load. The major watt-seconds loss is developed near the start of circuit opening even though a high-voltage kick develops just before the current reaches zero. This last kick, though occurring at so low current value that little wattage is involved is of importance because of the strain it places on circuit insulation and because it contributes, in some instances, to arc restriking or flashovers to ground.

In order that switch functioning under the previously described circuit conditions may be checked by laboratory tests it is necessary that voltage and current

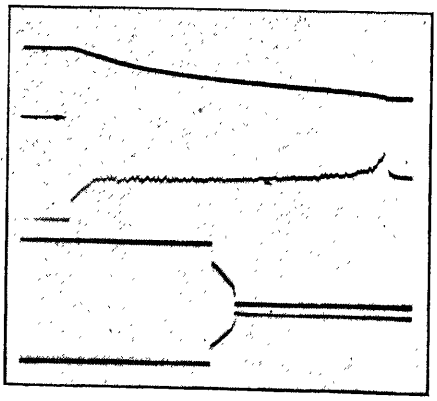

Figure 2. Current and voltage across switch c ntacts, 25 amperes inductive (above) and 30 amperes resistance (below) loads on 30-volt supply

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sources, along with elements to establish circuit components for inrush and inductive effects, be provided. A series of performance requirements, substantially formalized in specification *ANS*-20 is applied to test each switch intended for aircraft, establishing the following data:

1. Current-making capacity (10,000 operations).
(*a*). Lamp load (10× hot filament current).
2. Current-breaking capacity (10,000 operations).
(*a*). Resistance load.
(*b*). Inductive load (using standard test inductors with L/R ration of 0.26).
3. Emergency break (50 operations inductive load).
4. Current-carrying capacity.

1*a*. The inrush circuit provided for determining current-making capacity uses a normally closed contactor which opens soon after (about 40 milliseconds) the switch under test closes (see Figure 3) and remains open until the switch under test opens. As shown in Figure 4, the switch under test opens zero load. This may be made a finite load, however, by shunting the break load across this normally closed contactor. The advantage of this circuit lies in the fact that the line of current increase is almost vertical, and a flat top holds the inrush at maximum value for approximately 40 milliseconds so that any switch closing disturbance occurs at peak current. The lockout-contactor type of circuit is a more severe circuit than usually found in practice, but it provides a test that may be duplicated in any laboratory and determines a switch rating that may be used safely with any kind of inrush load.

2*a*. The resistance-load test requires no special comment.

2*b*. The inductive circuit for this type of testing was co-operatively developed by the Army Air Corps and the Navy Bureau of Aeronautics, again with the aim of providing a circuit which could be duplicated in any laboratory and which would, at one time, test switches under conditions of sufficient severity to guarantee satisfactory performance in any of the so-called "highly inductive" aircraft circuits. These circuits include brake coils, solenoids, relay and contactor coils, and so forth.

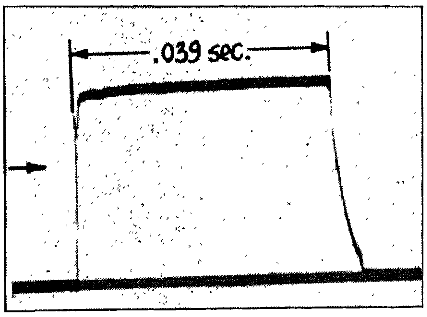
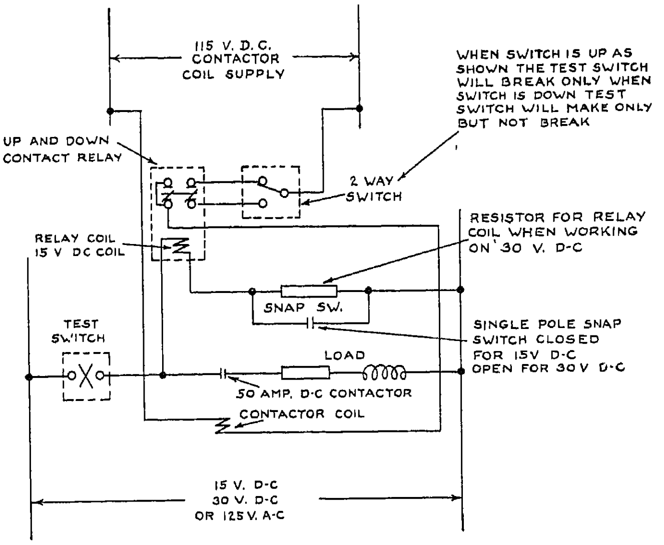


Figure 3. Wave form. Standard inrush circuit

Figure 4. Line diagram of standard inrush circuit


115 V. D.C. CONTACTOR COIL SUPPLY
WHEN SWITCH IS UP AS SHOWN THE TEST SWITCH WILL BREAK ONLY WHEN SWITCH IS DOWN TEST SWITCH WILL MAKE ONLY BUT NOT BREAK
UP AND DOWN CONTACT RELAY
2 WAY SWITCH
RESISTOR FOR RELAY COIL WHEN WORKING ON 30 V. D-C
RELAY COIL 15 V DC COIL
SNAP SW.
TEST SWITCH
SINGLE POLE SNAP SWITCH CLOSED FOR 15V D-C OPEN FOR 30V D-C
LOAD
50 AMP. D-C CONTACTOR
CONTACTOR COIL
15 V. D-C
30 V. D-C
OR 125 V. A-C

RELAY COIL MUST BE CHANGED WHEN CHANGING TO 125 VOLTS A-C ON TEST SWITCHES.

So duplication could be readily attained, the following inductor elements are defined:

1. Iron circuit.
(*a*). Lamination dimensions.
(*b*). Lamination material.
(*c*). Magnetic saturation.
(*d*). Air gaps.
2. Copper circuit.
(*a*). Coil dimensions.
(*b*). Ballast resisters.

The stored energy of the magnetic circuit and the rate at which this stored energy is released were made the subject of a study based on tests conducted on a number of solenoids, relay coils, and so forth, now in use. Voltage surge, current decay, and arcing time were the main measures of equivalence and were determined with the circuit controlled by the same switch type in all cases. The standard test inductor shown in Figure 5 resulted.

The functioning of a switch is basically dependent on the characteristics of the electrical circuit to be controlled, but the physical characteristics of the surrounding atmosphere and supporting structure are, in many cases, of equal importance.

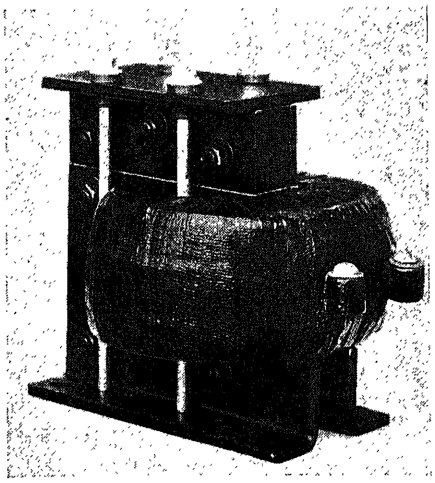


Figure 5. Standard test inductor

Physical conditions which at times assume such importance are:

1. Ambient temperature.
2. Humidity.
3. Chemical atmosphere or sprays.
4. Accelerations.
5. Altitude or pressure.

The ambient temperature, in the low range of 50 and 60 degrees Fahrenheit below zero may interfere with the mechanical action of a switch by congealment of lubrication, while the high range of 150–175 degrees Fahrenheit accentuates the problem of temperature control in current-carrying elements and insulation.

The second physical condition substantially affecting switch performance is the presence of humidity or moisture, either through exposure of the switch to direct wetting action by rain, splashing, and so forth, or by breathing air into the switch interior under conditions of high relative humidity with variable temperature. In the first case the switch is best protected by the use of water-resistant gasketing and joint constructions. In the

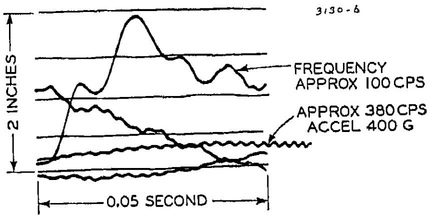


Figure 6. Panel subjected to shock test

With eight-pound shutter on bakelite panel at center second case, with moisture in a relatively tight switch interior by condensation of vapors entering the switch by breathing, either the provision of drain holes or a highly water-resistant construction is indicated.

The third condition involves the presence of salt spray or combinations of chemical atmospheres with heat or moisture that result in deterioration at metallic surfaces and junctions of case or mounting elements. The elimination of abutting unlike metals or metals with unlike plating is a partial solution to this problem although the protective adequacy of the plating as related to the base metal must be determined. Nickel on steel or cadmium on brass are not the best specific protective coatings for these two base metals, yet cadmium plating for assemblies of steel and brass parts is a good plating because the more readily corroded material (steel) is protected and unlike metallic abutments are not present. Choice of protective metallic coating must also recognize the by-products resulting from protective action. For instance, zinc, in the presence of moisture, forms zinc hydroxide and carbonate, bulky salts of high electrical resistance, that would cause failure in closely fitting assemblies or at the electrical contacts of a switch. Cadmium, in a tight enclosure and at high temperature, in presence of some insulating varnishes and oils, such as tung and chinawood oils and air dry phenolic varnishes, may "bloom," forming a bulky salt of high electrical resistance.

The fourth condition affecting switch performance results from mechanical acceleration which may occur in the following forms:

(a). Linear.
(b). Vibrational.
(c). Shock.

(a). In the first form a constant linear acceleration and its effect on switches can be readily computed and measured by laboratory tests. A rotating table may be used to generate this acceleration in accordance with the formula:

$$\text{Acceleration} = Kx(\text{RPM})^2 \times R$$

(b). In the second form of acceleration free vibrations that may be induced by vibration in the plane frame and structural elements are checked by test on a table oscillating in harmonic motion over a frequency range of 10 to 60 cycles per second with an over-all amplitude of 0.060 inch. Devices under test are checked for structural failures and electrical failures (closing circuit when open or vice versa) with the frequency range traversed at the rate of about once in 3-5 minutes for a period of one hour.

(c). In the third form of acceleration, shock resulting from landing, shell burst, and so forth, transmits itself to the switch through plane structure and switch mounting means. The motion, velocity, and accelerations involved are a combination of effects discussed in *a* and *b*, although the energy imput to the switch is ordinarily greater than in either *a* or *b*. See Figure 6 showing the time—motion trace of a panel subjected to a high-shock test. Manual switches have been tested in a pendulum shock machine. Consideration is being given the use of a drop machine suggested by the Bureau of Standards.

In these three acceleration tests, the measure of electrical failure is a self-maintaining electrical indicator set to pick up in four milliseconds and drop out in four milliseconds. A device like a bomb rack solenoid requiring eight milliseconds to initiate armature movement, would not be affected when in circuit with a switch which passes a shock test with four millisecond indicating means.

The fifth physical condition affecting switch performance is altitude or pressure. Air-gap dielectric strength becomes a matter of definite concern. The sparking potential, a function of gap and pressure, becomes a major factor, and switches with normal forms of air gaps at contacts and terminals, capable of holding 2,500 volts pressure across these gaps at 29 inches of mercury, have by test failed at 800 volts in pressure equivalents of five inches of mercury. In the presence of such a substantial drop in sparking potential any tendency toward flashovers to ground or across polarity during circuit opening is severely emphasized, and the interrupting capacity of switches, particularly on inductive circuits, must be carefully determined.

Aircraft Inverter Construction

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THE word "inverter" is a generic term used to designate any device which receives direct current and delivers alternating current. Such a device may consist of electronic elements, or vibrating contacts, or mechanical choppers, or rotary types. The scope of this paper will be confined to a discussion of rotary-type conversion equipment.

Various classes of rotary inverters and control accessories will be listed with a brief and general description of characteristics and relative merits, viewed from the standpoint of the requirements of military aircraft. A statement of what the principal requirements are is first in order.

Operating Requirements

An aircraft inverter must operate on the d-c supply of the plane, which usually is obtained from generators driven from the main engines. These generators may be of 50-, 100-, or 200-ampere rating; and in a four-engine bomber there are usually four 200-ampere generators connected in parallel while in normal flight. The d-c bus voltage is maintained fairly constant by means of carbon-pile regulators controlling the generator fields. A battery usually floats on the bus.

The inverter is connected to the d-c bus through a solenoid-operated switch. For purposes of inverter design, the voltage delivered to it is considered to be 27.5 volts, with maximum variation of plus or minus 2.5 volts. In routine flight, carbon-pile voltage regulators restrict the d-c voltage variation to less than one volt, except for momentary transients.

With an input voltage at any point from 25 to 30 volts, the inverter should be capable of starting and carrying full rated load continuously. An operating life of at least 1,000 hours without major overhaul is expected, with starting as frequent as once per hour.

Output ratings may be anywhere from 6 to 3,000 volt-amperes, with an ever-present possibility of increased loads in the future requiring larger sizes. Effort has continuously been made and will continue to be made to standardize on rated loads or sizes. In view of the fluidity of the load requirements and resulting necessity of modifying specifications on actual conversion units procured from time to time, a formal tabulation of ratings will not be listed. Formalized specifications are available, one of which is the United States Army Specification 94-32270. These formal specifications are amended from time to time as need requires.

In order that this discussion may be self-contained however, three sizes of

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inverters currently used in large quantities will be mentioned, the ratings of which are as follows:

	Output at 400 Cycles		
Rating	26 Volts 40 Per Cent Power Factor	115 Volts 90 Per Cent Power Factor	Class*
250 va..	60 va..1 phase..	190 va..1 phase..	*C*
750 va..	250 va..1 phase..	500 va..1 phase..	*D*
1,000 va..	250 va..1 phase..	750 va..3 phase..	*F*

* See tabulation under "Classification of Basic Types."

The 26-volt output is primarily intended for electrical-instrument supply but may be used for other single-phase loads such as fluorescent lighting. The voltage requirements are not exacting on this circuit, and automatic regulation has not been considered necessary.

The 115-volt circuit may be used to supply fluorescent lights, automatic radio compass, motor loads, supercharger regulators, automatic pilot, and bombsight. In the case of some of these loads, the voltage requirements are not exacting, and the percentage variation experienced on the d-c bus may be tolerable. In such case, an inverter which may be regarded as a transformer (if of sufficiently low internal impedance) may be a simple and satisfactory mechanism.

However, as more precision equipment is developed and put into use, and with transient voltage fluctuations occurring because of high momentary current to gun-turret motors, need for automatic control of the 115-volt 400-cycle source becomes apparent.

As a broad statement, the voltage on that circuit should not exceed 120 nor be less than 110 at any load from no load to full load, and at any d-c input voltage from 25 to 30. However, modulations or transients or poor wave form may be objectionable in some instances. Since no practical source will deliver a pure sine wave at exactly 115 volts 400 cycles in service, and since the greater precision demanded the more complicated the equipment becomes, the construction and acceptance of the most practical machine in any particular case necessarily becomes a matter requiring engineering cooperation. In other words, there are so many factors involved that the procurement engineers cannot say "Here are our specifications. Any machine meeting them is acceptable, and contrariwise." Likewise, the manufacturers' engineers cannot say "Here is the best possible equipment for the job; that's all there is."

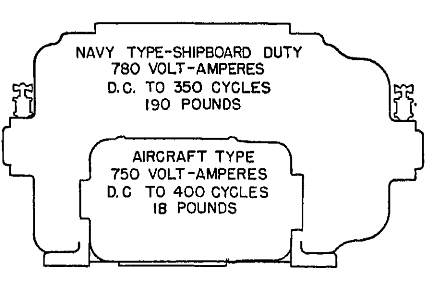

Figure 1. Comparison of size and weight of units built for aircraft and shipboard duty

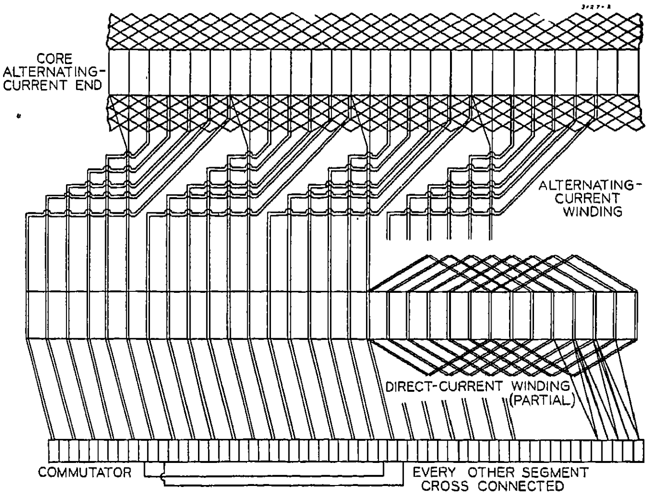
CORE
ALTERNATING-CURRENT END
ALTERNATING-CURRENT WINDING
DIRECT-CURRENT WINDING (PARTIAL)
COMMUTATOR
EVERY OTHER SEGMENT CROSS CONNECTED

Figure 2. Cascade inverter—schematic diagram of armature connections

Specified weights for various inverter ratings will not be listed, as they are changing from time to time. Refinements in design make lighter weight machines available. However, a contrary tendency toward heavier weights also inevitably results from requirements for more accurate control of output voltage and frequency. As an illustration of weight trend, one inverter of the inductor-dynamotor type was in use in 1939, weighing 18 pounds and capable of delivering 250 volt-amperes. A cascade-type inverter was designed in 1940 having the same weight and delivering 750 volt-amperes. A comparison of the latter with a motor-generator set for use on board ship is given by Figure 1, where the two are shown with outlines drawn to the same scale.

Later developments, however, required addition of radio-interference filter and control accessories, adding a few pounds.

The importance of light weight in aircraft equipment needs no argument. One pound of fixed weight requires another pound of structure and fuel to carry it.

Likewise, the importance of efficiency may be emphasized by the following approximate data:

An aircraft engine consumes about 0.5 pound of fuel per horsepower-hour. The generators are roughly 75 per cent efficient. Hence, 1,000 watts of inverter input requires the consumption of about one pound of fuel per hour. Every 100 watts saved is worth a pound of fuel on a ten-hour flight. That pound of fuel might take the airplane another half mile. It might be said as a generality that 100 watts of additional inverter losses would necessitate a certain additional weight of generator capacity. In a practical case, this capacity may be already available.

All things considered, perhaps a weight saving of one pound in an inverter is worth while if the losses are not increased as a result by more than 100 watts. This figure is by no means definite, however, and has not been set up as a design criterion.

It is no simple problem, nor can any definite general answer be given, as to what point it is best to stop at in weight reduction on account of loss in efficiency or possible sacrifice in reliability. Likewise, extremely light weight may mean high internal impedance and hence high-voltage regulation. High speed makes light weight possible but reduces bearing life and decreases reliability.

These are all matters which should have the benefit of sound engineering judgment. The most sound judgment is

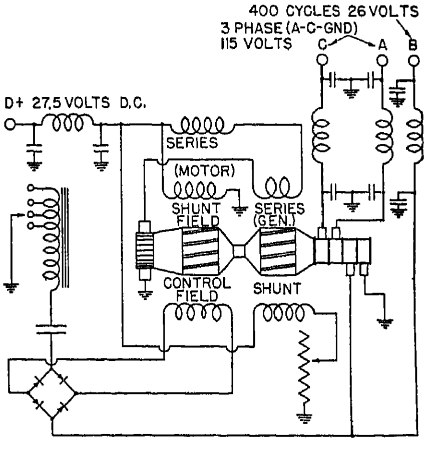

Figure 3. Schematic diagram of 1,000-volt-ampere dynamotor-generator type

based on experience. It is quite common in aircraft work, however, for experience to bring to light considerations which either were not anticipated or were erroneously regarded as of negligible importance.

Classification of Basic Types

A rotary inverter essentially consists of a d-c motor driving an alternator. The two elements may be combined in a single magnetic circuit, the most intimate combination being an inverted rotary converter. The latter is analogous in some degree to an autotransformer, where primary and secondary conductors are common. A simple rotary converter cannot be used to fulfill the requirements in question, however, on account of the voltage ratio involved and the fact that grounded circuits are used on both primary and secondary.

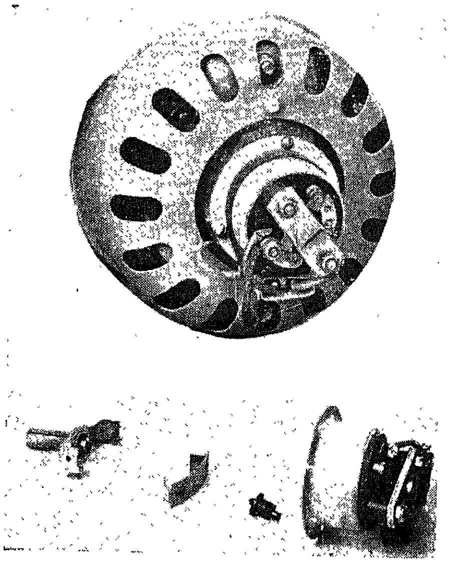

Figure 4. Centrifugally operated contact-making speed governor, cover removed

Parts shown in lower view include two rotating pieces, bakelite button for transmitting thrust, and stationary contact unit with adjusting screw

The following list covers classes of equipment in current production, types which have been built experimentally, and others which conceivably could be used. The list does not include every theoretical possibility of variation and combination.

A. Motor-generator set
  1. M-G set with rotating field generator
  2. M-G set with rotating armature generator
  3. M-G set with inductor generator
  4. M-G set with permanent magnetic generator

B. Dynamotor

C. Inductor-dynamotor

D. Cascade inverter

E. Inverted rotary converter with transformer
  1. Separate transformer
  2. Rotating transformer

F. Dynamotor-generator

G. Dynamotor with booster

H. Cascade inverter with booster

Types *A*4, *B*, *C*, *D*, and *E* are not adaptable to independent output voltage control, except by a separate series boost or buck arrangement in primary or secondary circuits. The behavior of *B*, *C*, *D*, and *E* types may be compared with that of a transformer, where output voltage is proportional to input voltage less internal impedance drop.

Considering the other classes where automatic control of output voltage may be obtained by field regulation, these may in turn be divided into two groups—those with slip rings and those without.

*A*1, *A*2, *F*, and *G* have slip rings.

*A*3 and *H* do not have slip rings.

Of these latter two groups, those without slip rings will weigh perhaps 25 to 35 per cent more than those with slip rings, all design factors being on an equivalent basis.

Another theoretical possibility which would fall in the last group is an inductor dynamotor with inductor-type booster generator. The practical advantages of such a combination is questionable.

The motor-generator set with inductor generator, class *A*3, has the advantages of ruggedness and reliability, since it has no conductors on the generator rotor and no slip rings. For 400-cycle service where light weight and hence high speed is required, the generator will probably be the twin-stator type with single stationary field coil in the shape of an annular ring lying between the two stators. The generator rotor will have three salient poles or six effective poles on each end, for 400 cycles at 8,000 rpm. The direction of the flux is centrifugal in one stator and centripetal in the other, and crosses each air gap in three rotating bands of high and essentially constant density at the rotor pole faces.

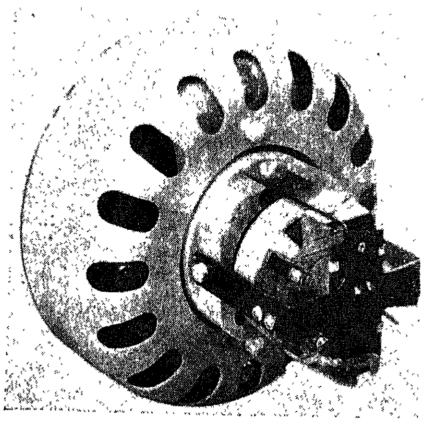

Figure 5. Centrifugally operated carbon-pile speed governor

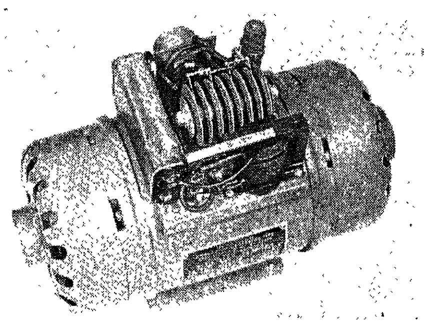

Figure 6. Physical arrangement of resonant-circuit feed-back speed-control equipment including capacitor, reactor, and rectifier shown in Figure 3

The inductor dynamotor, class *C*, has a single armature with salient teeth or poles. The a-c winding lies in slots in the pole faces, the field poles otherwise being like those of an ordinary two pole d-c motor. This mechanism is essentially a merging of the motor-generator set with inductor generator into a single magnetic circuit. This combination does not produce a net gain necessarily, since control of output voltage (except in steps by a tap switch) is lost, and a speed of over 4,000 rpm for 400 cycles would result in difficulties. For some purposes however, such a machine has been extensively used with satisfactory results.

The cascade inverter consists of two parts in one frame and is physically similar to a two bearing motor-generator set. The d-c end acts as an inverted rotary converter built for 200 cycles, multiphase output. This output is delivered to the rotor of the a-c end, which is slotted and wound similar to the d-c armature, except that the 200-cycle phase rotation is reversed, as shown by Figure 2. Thus, the a-c end operates as an a-c excited generator, with combined electrically and mechanically rotating field. When built for a speed of 6,000 rpm for 400-cycle output, half of the energy is delivered from the d-c end electrically and half mechanically. There are four conventional stationary salient field poles on the d-c end, and a four-pole stator on the a-c end. The only control of output voltage possible is obtained by a tap switch connected to the output stator.

An inverted rotary converter with rotating transformer, class $E2$, has been built, the advantages conceived being that the (circular three-legged) transformer would be well cooled; and six-phase low voltage could be carried directly to the transformer primary winding. This particular machine was intended for three-phase output only and hence had three slip rings.

The dynamotor-generator, class F, was designed to deliver both three-phase and single-phase output, and it was desired not to have the single-phase load unbalance the three-phase voltages. Accordingly, the motor was designed for operation at 8,000 rpm with six poles, and a dynamotor winding superimposed to deliver single-phase 400 cycles through slip rings. Figure 3 is the schematic diagram. The generator is a three-phase rotating-armature type. Hence this machine is a combination of class $A2$ and class B.

A dynamotor with booster, class G, has been built for three-phase output only. A dynamotor may be somewhat lighter than a motor-generator on account of having a single magnetic circuit, utilizing the same flux for both motor and generator conductors. A small generator adequate to supply the differential energy required to maintain constant output voltage is provided, acting as a booster. The greater part of the energy conversion takes place in the dynamotor. This was built in two and three kilovolt-ampere sizes and is probably the lightest type of inverter of the slip ring class for this particular duty.

A cascade inverter with booster, class H, has been built, which is a class D (cascade) inverter with tertiary element in the 200-cycle circuit between the d-c and a-c armature cores. This tertiary element is a small 200-cycle generator with four salient field poles and with each armature coil connected in series with the 24-wire 12-phase 200-cycle output from the d-c armature. The series boost effect at 200 cycles was added at an advanced phase angle so that with a lagging 400-cycle load the power factor of the 200-cycle load on the d-c armature might be brought near unity. The objective was to increase the efficiency since an inverted rotary converter operates most efficiently on a resistance load. However, the net over-all gain of this boosting at an advance angle is dubious, since the tertiary element must then have more capacity than would be the case for in-phase boosting.

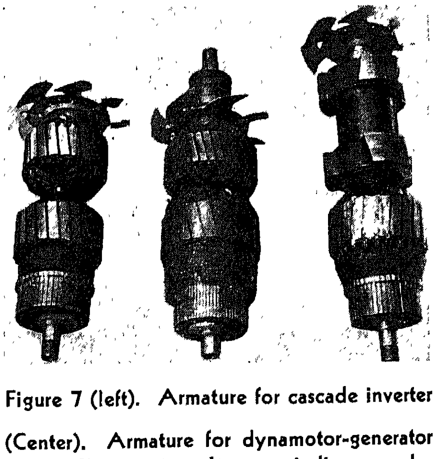

Figure 7 (left). Armature for cascade inverter

(Center). Armature for dynamotor-generator type. Twenty-six-volt a-c winding can be seen in the same slots with d-c motor armature winding

(Right). Rotating element including motor armature and rotor for inductor generator

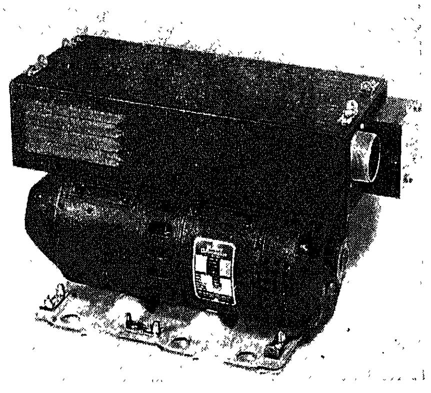

Figure 8. Inverter made by the Leland Electric Company

An inverter with inductor-type generator is being considered, with one stator having a three-phase winding and the other supplying the single-phase load.

At least four of the classes listed are in current use in large quantities.

As is frequently the case, there would be no particular problem involved if one type of machine had all of the advantages (minimum number of brushes, light weight, reliability, and independent sources of single- and three-phase output). Many practical considerations enter, and it may be that, even for a single well-defined set of load requirements, it will be difficult if not impossible to arrive at a universally accepted decision as to the best machine for the job.

The problem of obtaining satisfactory brush operation at high altitudes is now generally recognized in the electrical industry as a serious one. The approach to this, in so far as inverter design is concerned, has been to study the problem intensively in itself but simultaneously to develop inverter designs without generator slip rings. As indicated previously, a sacrifice in weight must be accepted in payment for the increased reliability of an inverter with no generator brushes.

Construction Details

Inverters for aircraft duty are made in two-bearing open-type frames. Specifications require openings to be small. Internal fans, either propeller or centrifugal type, are used. Sealed or labyrinth-type shielded bearings are generally used, with no provision for relubrication. A speed of 8,000 rpm is commonly used, but cascade types running at 6,000 rpm and inductor types at 4,000 are also widely used. The inverters must be capable of operating in any position, be built to withstand shock and vibration, and be capable of operating in any outdoor temperature and pressure encountered on the surface of the earth and up to 40,000 feet above it.

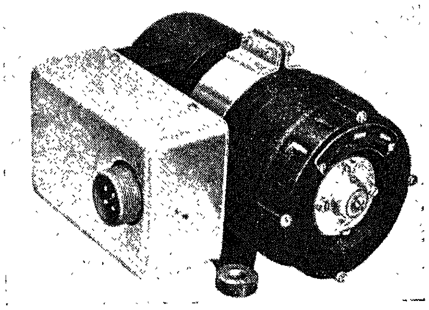

Figure 9. Inverter of Eclipse aviation division, Bendix Aviation Corporation

Hollow shafts are used in many of the inverters for weight reduction. End bells are made of magnesium or aluminum. Armature and stator laminations have holes or slots where all metal not definitely needed for carrying flux or for mechanical purposes is removed.

Armatures are, of course, dynamically balanced. In the case of some of these machines where the armatures are long, the shaft not too heavy, speed high and air gap small, special procedure may be required to insure that a static unbalance near the middle is not compensated by two weights added on the opposite side but near the bearings, resulting in a tendency for the shaft to spring.

With good air circulation easily obtainable at the high speeds used, and the losses kept relatively low to obtain good efficiency, it has not generally seemed necessary to provide for higher temperatures than commonly dealt with in other electrical apparatus.

Filters for suppression of radio interference are provided integral with the inverters.

Electrical connections are made with

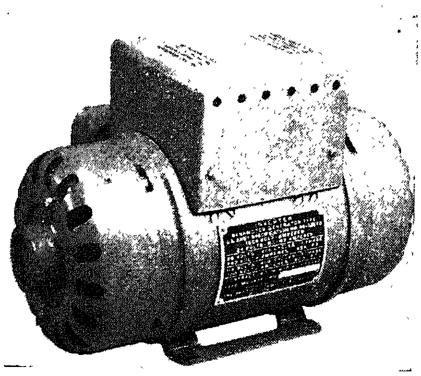

Figure 10. Holtzer-Cabot inverter

standardized *A-N* aeronautical-type fittings.

Frequency Control

Figure 4 shows a centrifugally operated contact-making speed governor which has no slip rings or brushes. The rotating element consists of a yoke carrying a spring which is deflected by the centrifugal force resulting from two weights. The axial motion of this spring is transmitted to contacts which are a part of the stationary unit by means of a "bakelite button" which engages with a dimple in the spring at the center of rotation. The points are of molybdenum-silver. The usual capacitor and resistor are provided across the contacts. The familiar Lee-type governor is also used.

A carbon-pile speed governor which is actuated by centrifugal force has been developed, the operation of which is superior to that of the contact-making governor in some respects. This is shown by Figure 5.

A resonant circuit feed-back speed-control circuit, Figures 3 and 6, is used on one inverter. This has a series circuit tuned for a peak at slightly over 400 cycles. In series with the capacitor and reactor in this circuit is a dry-disk rectifier. The d-c terminals of this in turn supply a control field on the motor. Consequently, as the output frequency approaches 400 cycles, the current in this control field rapidly increases, and the speed stabilizes. The rectifier delivers to the control field direct current plus an 800-cycle component. In order to maintain substantially constant flux, each field pole is provided with a heavy copper shading coil. Taps are provided on the reactor to compensate for manufacturing variables in the inverter and the capacitor.

Electronic controls have been developed for both speed and voltage. It would be quite possible to obtain extremely accurate frequency control by this means, if required, by using a tuning fork as the reference. Here again however, the greater the precision required the more susceptible to trouble the apparatus becomes—as well as being more bulky.

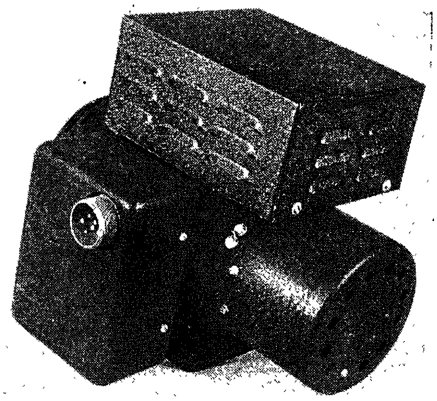

Figure 11. Inverter manufactured by General Electric Company

Voltage Controls

It is believed that no good purpose will be served by elaborating on theoretical possibilities for automatic voltage control. A long discussion could be presented, starting with the Tirrill-type regulator and leading up to electronic-type precision controls. These devices would include multifinger contact-type regulators, saturated reactors, and various others. The carbon-pile regulator, however, has come into very general use both for controlling the main d-c generators and for regulating the 400-cycle inverter output as well. Present indications are that it has no inherent serious disadvantages and that it produces satisfactory results. In applying a carbon pile to an inverter, over-all temperature compensation must be obtained, including the rectifier in the circuit supplying the actuating solenoid from the 400-cycle circuit being regulated.

Radio-Interference Filter

Specifications require the radio-frequency component of voltage in any input or output conductor of the inverter to be less than 50 microvolts at any frequency from 0.2 to 20 megacycles. For the purpose, it is common practice to use a π-circuit filter in each conductor. The reactance should not be so large as to cause excessive voltage drop in the 400-cycle conductors, and here air-core chokes are commonly used. Textbook rules for the most efficient design of choke result in minimum space and weight. Capacitors should be no larger than necessary, both because of space and weight considerations and the effect of the leading current on the no-load voltage where voltage regulators are not used. Great care must be used in designing a filter as to arrangement of components, length of leads, and method of grounding.

Conclusions

While continuous effort has been made to standardize ratings and performance requirements, this has been difficult. Load requirements change rapidly, and the way must be left open for refinements of inverter design.

A motor with automatic speed control driving a generator with automatic voltage control appears to be the most desirable general type for most purposes.

Production of existing designs which have proved to be usable must be continuously carried on; and simultaneously new designs must be studied and advantages and disadvantages weighed. Interchangeability must be considered. Also, designs cannot be changed too frequently on account of maintenance problems, spare parts for aircraft components now being carried at many points around the world.

Fundamental Principles of Amplidyne Applications

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ASSOCIATE AIEE

Synopsis: The Amplidyne generator has been widely applied and is recognized as an important device in the electrical industry. Since it is used primarily as a power amplifier in regulating systems, the requirements of an amplifier for this use are analyzed, and the characteristics of the Amplidyne generator are compared in this paper with these requirements.

Owing to the large number of distinct windings of the Amplidyne, it is desirable to follow a consistent practice in terminal markings and polarities. Proposals for this purpose are outlined in the paper, and typical values of amplification for different output ratings are given.

Service experience and an application typifying the use of the Amplidyne are discussed, so that the reader may become familiar with the device and its uses.

SINCE its introduction into the electrical industry, the Amplidyne generator has been found useful in a large variety of applications. The chief use has been in closed-cycle controllers or regulating systems as a power amplifier. In order to see why the Amplidyne is particularly adaptable and useful in regulating systems, it is desirable to review and analyze the characteristics of the regulating system to determine the requirements of an amplifier for use therein.

The Regulating System

A closed-cycle control system or regulating system is one in which the controlling agency is actuated by some function of the final output in such a manner as to minimize any deviation of the output from an ideal value.

There are three fundamental elements of such a system, namely:

1. A standard of ideal performance against which the output to be regulated can be compared.
2. An amplifier to amplify any deviation of output from the standard.
3. A means of feeding the amplifier output into the system in such a manner as to minimize the deviation.

Many factors need to be considered in devising a regulating system, and these have been outlined in a previous article.[1] It is not the purpose here to elaborate on these factors any more than necessary to establish a basis for analysis of the amplifier requirements in the system and to show how such an analysis acts as a guide in evaluating various proposed amplifiers for this use.

The first element of the regulating system, namely, the standard of performance, may be either a primary or a secondary standard and must be of like physical kind to the quantity to be compared with it. It is very often necessary to transform the quantity to be regulated into the same form as the standard for comparison. It can be readily understood that standards and transformations can most readily and economically be obtained at low power levels.

The third element of the system, the means of feeding the amplifier output into the system, may, and in most cases does, require a very considerable amount of power.

It is obvious that a high degree of power amplification is an important requirement for an amplifier to be used in a regulating system.

Stability is another primary requirement. It is beyond the scope of this article to establish mathematically the requirements for stability of the regulating system. Instability can be explained by the existence of a series of time delays in the system. In general, the greater the number of time delays in series and the higher the amplification the greater will be the tendency to oscillate. Also, in a system with three or more simple exponential time delays in series, the more nearly the time delays equal each other in magnitude the greater will be the tendency to oscillate. Since in the usual industrial power apparatus the time delay of the final controlled element is appreciable, it is generally important to reduce to a minimum the time delays in the system prior to the final one. From these considerations, the second desirable characteristic of the amplifier is as short a time delay as possible between input and output.

One thing that has not been generally recognized is that the closed regulating circuit affects the response time of the system as well as minimizing the deviation between the standard and the system output. In this respect, a regulating system is very similar to an inverse feedback amplifier.

Analysis of Simple Regulating System

Figure 1 illustrates a very simple regulating system with one time delay. For purposes of analysis, assume that up to a time $t=0$ the standard voltage has been 0 and is instantly made equal to E_S at time $t=0$. Furthermore, assume the amplifier and generator amplifications to be linear over the range of voltage considered.

Let

R_1 = generator field resistance
L_1 = generator field inductance
K_1 = generator armature voltage per field ampere
M_1 = amplifier amplification
$T_1=\frac{L_1}{R_1}$ generator field time constant

$$E_1=-M(E_R-E_S)=(R_1+L_1p)i_1$$

$$E_R=Ki_1$$

$$-ME_R+ME_S=(R_1+L_1p)\frac{E_R}{K_1}$$

$$\frac{E_R}{K_1}R_1(1+T_1p)+ME_R=ME_S$$

$$E_R=\frac{K_1ME_S}{R_1\left(\frac{MK_1}{R_1}+1+T_1p\right)}$$

Let

$$\alpha=\frac{K_1M}{R_1}=\text{over-all system amplification}$$

Then

$$E_R=\frac{\alpha E_S}{\alpha+1}\left(1-\epsilon^{-\frac{(\alpha+1)t}{T_1}}\right)$$

If we had merely applied a voltage E_1 to the field of the generator sufficient to bring the output voltage without the regulating system up to a value equal to E_R, the equation for E_R would have been

$$E_R=\frac{E_1{}^1K_1}{R_1}\left(1-\epsilon^{-\frac{1}{T_1}t}\right)$$

Thus, we see that the use of the regulating system not only minimizes deviation from the value of the standard but has reduced the time delay of voltage build-up or effectively decreased the time constant in the ratio of $\alpha+1$ to one.

This analysis assumes linearity of amplification, and it is to be noted that if the voltage output of the amplifier is limited,

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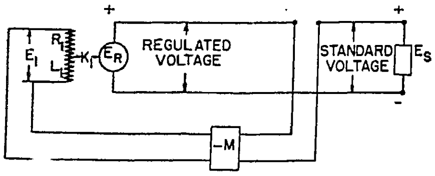


Figure 1. Simple regulating system

the response will be determined by the maximum voltage available at the generator field.

The Amplidyne

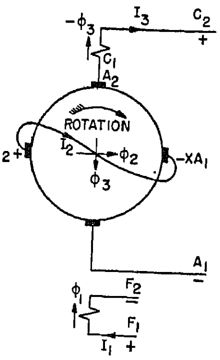
The Amplidyne has been described in detail in other articles.[2-4] It is here desired to point out the features of its design which make it outstanding in its performance.

A review of the operation is desirable to this end. Figure 2 shows a two-pole d-c armature with two pairs of brushes in quadrature. The frame is omitted from the diagram for simplicity, and the assumption is made that commutation is ideal. A flow of current I_1 in the control field F_1F_2 in the load axis creates a load-axis flux ϕ_1. The armature conductors cut this flux, and a voltage E_2 is generated in them to send current I_2 through the brushes of the short-circuit axis. The flow of armature current I_2 through the short-circuit axis of the armature develops a distributed magnetomotive force and flux ϕ_2 in the short-circuit axis. Rotation of the armature in this field generates a voltage E_3 at the load-axis brushes. The magnetomotive force of the armature caused by current I_3 is in opposition to the magnetomotive force of the load-axis control field. However, there is a distributed compensating field which neutralizes the armature magnetomotive force caused by current I_3. Only a very small magnetomotive force is required in the load-axis control field to cause generation of the required voltage and current in the short-circuit axis to give full output voltage at the load terminals. Only a small generated voltage is necessary in the short-circuit axis, and yet the full voltage of the armature is available for forcing a rapid rise or decay of flux ϕ_2.

Analogy and Analysis of Time of Build-Up

The Amplidyne combines into one machine two stages of amplification, each stage of which has a very small time delay. The first stage of amplification is from the load-axis control field to the short-circuit axis, and the second stage is from the short-circuit axis to the load terminals. For purposes of approximate

Figure 2. Schematic diagram illustrating operation of the Amplidyne



analysis, it is as though two d-c generators were in series and with the first generator just as large as the second as regards voltage output capability. This is illustrated in Figure 3.

Let a unit voltage E_1 be suddenly applied to the control field.

Let $T_1=\frac{L_1}{R_1}K_1=$volts E_2 generated per field ampere i_1

$K_2=$volts E_3 generated per field ampere i_2

$$T_2=\frac{L_2}{R_2}$$

Then

$$E_1=(R_1+L_1p)i_1$$

$$E_2=K_1i_1=\frac{K_1E_1}{R_1+L_1p}=(R_2+L_2p)i_2$$

$$E_3=K_2i_2=\frac{K_1K_2E_1}{(R_1+L_1p)(R_2+L_2p)}$$

$$E_3=\frac{K_1K_2E_1}{R_1R_2(1+T_1p)(1+T_2p)}$$

The solution of this is

$$E_3=\frac{K_1K_2E_1}{R_1R_2}\left[1+\frac{T_1}{T_2-T_1}\ \epsilon^{-\frac{1}{T_1}t}-\frac{T_2}{T_2-T_1}\ \epsilon^{-\frac{1}{T_2}t}\right]$$

Figure 4 is a plot illustrating the voltage rise in such a circuit having constants as follows:

$T_1=0.058$ second $T_2=0.092$ second

These are values taken from a typical 1,500-watt 250-volt 1,750 rpm Amplidyne. The time delay T_1 is for a field occupying 25 per cent of the field coil space and capable of carrying continuously ten times the current required to excite the machine to 80 per cent voltage at rated load. The time delay T_2 is on a machine with a series field in the short-circuit axis which is connected in the armature circuit of the short-circuit axis.

From this analysis, it is evident that the Amplidyne is an ingenious method of obtaining two-stage amplification with a fast response in a single unit. A number of independent load-axis control field windings can be used, so that the resultant control ampere turns will represent the algebraic sum of several independently selected functions.

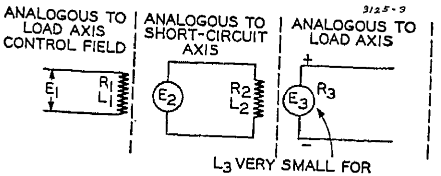

3125-3
ANALOGOUS TO LOAD AXIS CONTROL FIELD
ANALOGOUS TO SHORT-CIRCUIT AXIS
ANALOGOUS TO LOAD AXIS
E_1
R_1
L_1
E_2
R_2
L_2
+
E_3
R_3
-
L_3 VERY SMALL FOR COMPENSATED ARMATURE

Figure 3. Two-machine equivalent of the Amplidyne

Circuit Diagrams

To take full advantage of these possibilities and to avoid confusion in circuit diagrams, the symbols shown in Figure 5 have been found convenient. These symbols may be used as a guide for any combination of field windings. Connections of typical machines are shown in the diagram, and it can be seen that they are relatively simple. The only external connections are those of the various load-axis control fields and the power output leads.

Terminal Letters and Nomenclature

The same general rules are used for designating Amplidyne terminals as those in use for other d-c machines. The prefix X is added to the terminal markings of all windings which magnetize in the short-circuit axis, while the windings which magnetize in the load axis are marked without a prefix. See Figure 5 for detailed definitions of the various symbols.

Throughout the paper, the terms *load*

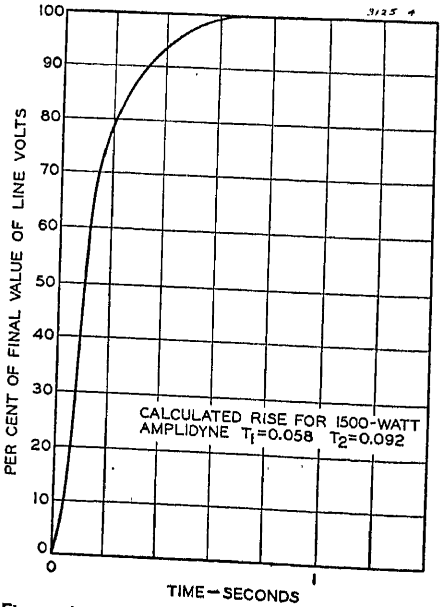


Figure 4. Response curve of typical Amplidyne generator

axis and *short-circuit* axis are used to describe the two axes. Other publications have also referred to the *load axis* as the *secondary axis* and the *direct axis*, and to the *short-circuit axis* as the *primary axis* and the *quadrature axis*.

Polarity

Amplidynes are so marked that for current which flows from the lower to the higher subscript terminal, in any of the control fields which magnetize in the load axis, the polarity of the terminal A_1 of the armature winding of the load axis is negative. This is true for both directions of rotation. Refer to Figure 5 for a detailed explanation of various windings.

Methods of Rating

Amplidyne generators are rated in kilowatts output, the product of rated voltage and rated current. It has been found desirable to select the voltage rating so that the saturation curve is essentially a straight line up to 80 per cent of rated voltage, and to provide a ceiling voltage approximately equal to 120 per cent of rated voltage. Typical saturation curves are shown in Figure 6. Usually, amplidyne generators are designed for 40 degrees centigrade temperature rise at rated voltage and current and to withstand a 200 per cent current peak momentarily at rated voltage. The machines should preferably be subjected to not more than 150 per cent rated current at rated voltage, tapering to 250 per cent at zero voltage.

The control field excitation requirements for typical 1,800-rpm Amplidyne-generator ratings between $^1/_2$ and 25 kw vary from 0.25- to 0.75-watt excitation (in one-fourth field space) with rated current output and 80 per cent rated volts; giving power amplifications (for one-fourth field space) from 2,500 to 40,000

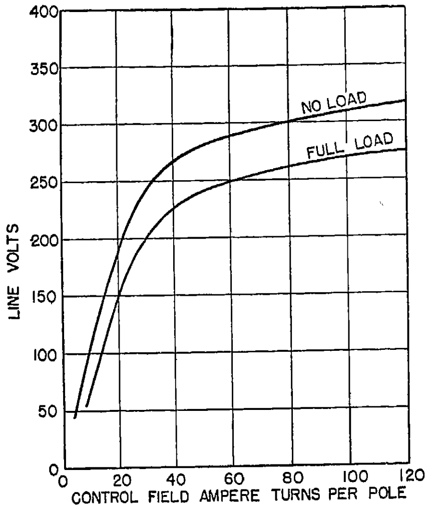


Figure 6. Typical saturation curves for 1,725 rpm 250-volt Amplidyne

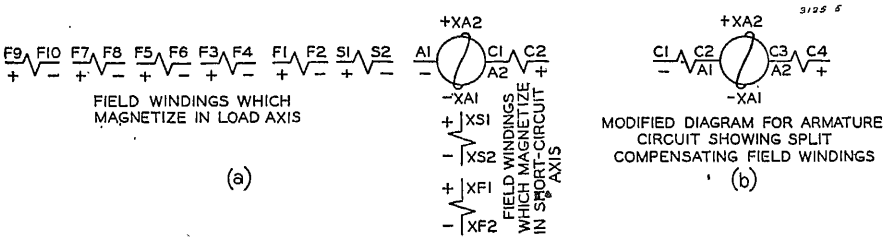

3125 6
F9 F10 F7 F8 F5 F6 F3 F4 F1 F2 S1 S2
+ − + − + − + − + − + −
FIELD WINDINGS WHICH MAGNETIZE IN LOAD AXIS
(a)
+XA2
A1
C1 C2
A2
−XA1
+ XS1
− XS2
+ XF1
− XF2
FIELD WINDINGS WHICH MAGNETIZE IN SHORT-CIRCUIT AXIS
+XA2
C1 C2
A1
C3 C4
A2
−XA1
MODIFIED DIAGRAM FOR ARMATURE CIRCUIT SHOWING SPLIT COMPENSATING FIELD WINDINGS
(b)

Figure 5. General diagram for clockwise rotation of Amplidyne generators

1. Compensating field windings

The compensating field windings, C_1C_2, are always connected in series with the load axis of the armature winding. Frequently it is convenient to split the compensating field winding so that half is connected in the circuit on each side of the load axis of the armature winding, and the diagram is modified as shown in diagram *a*

2. Commutating field windings

If a commutating field winding is used it will be connected in the armature circuit between terminals *A*1-*A*2 or *XA*1-*XA*2

3. Field windings in the load axis

Each terminal of the field windings in the load axis is marked with a single letter and subscript number. Exciting current which flows from the lower to the higher numbered subscript in any one of these windings produces a magnetomotive force and flux along the load axis and a speed voltage in the armature winding of the short-circuit axis of such polarity as to make XA_2 positive and XA_1 negative with clockwise rotation of the armature. The use of the letters and subscripts has additional significance:

S_1S_2, S_3S_4, and so forth, field windings intended for series connection; usually in the armature circuit of either the load axis or the short-circuit axis

F_1F_2, main control shunt field

F_3F_4, reference or counter shunt field

F_5F_6, auxiliary control shunt field

F_7F_8, antihunt shunt field

F_9, F_{10}, F_{11}, F_{12}, and so forth, shunt fields for other purposes than those already noted

4. Field windings in the short-circuit axis

The terminals of the field windings in the short-circuit axis are marked the same as the field windings in the load axis except that the prefix letter *X* is added. Exciting current which flows from the lower to the higher numbered subscript in any one of these windings produces a magnetomotive force and flux along the short-circuit axis and a speed voltage in the armature winding of the load axis of such polarity as to make A_2 positive and A_1 negative with clockwise rotation of the armature

The use of the letters and subscripts has added significance:

XS_1XS_2, and so forth, field windings intended for series connection; usually in the armature circuit of either the short-circuit axis or the load axis

XF_1XF_2, and so forth, field windings intended for shunt connection

5. Killer windings

A killer winding is sometimes used in special cases to reduce the residual. This may be a special winding or it may be special taps on a winding designed for other purposes. In either event the terminals are marked K_1K_2. Since the exciting current is alternating, the polarity marking is omitted

6. Counterclockwise rotation

The same letters and subscripts are used for both rotations. For counterclockwise rotation the polarities of all field windings and of the armature winding of the load axis remain the same as for clockwise rotation. The polarities of all field windings and the armature winding of the short-circuit axis are reversed. All interconnections of the various windings are the same for both rotations; except that field windings in the load axis which are connected in the circuit of the short-circuit axis, and field windings in the short-circuit axis which are connected in the circuit of the load axis must be reversed when rotation is reversed

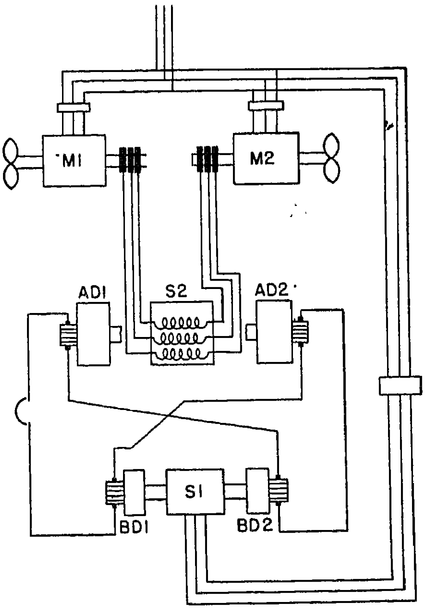


Figure 7. Schematic connection diagram with double motor drive using counterrevolving propeller

*M*1 and *M*2 = 22,000-horsepower slip ring 327-rpm fan motors

*AD*1 and *AD*2 = 2,500-kw d-c generators

*BD*1 and *BD*2 = 3,200-horsepower d-c motors

*S*2 = 20,000-kva 540-rpm synchronous motor

*S*1 = 6,000-kva 514-rpm synchronous generator

Guide for Wartime Conductor Temperatures for Power Cables in Service

AEIC SUBCOMMITTEE ON
WARTIME TEMPERATURES FOR CABLE CIRCUITS

Preface: The cable engineering section of the Association of Edison Illuminating Companies recently sponsored a guide particularly for the use of its member power-utility companies in the wartime operation of electric cables of the types as used mainly in underground systems. A large share of the underground cable circuits in this country are operated by the AEIC companies. The details of preparing the guide for the consideration, revision, and approval of the section were handled by a special subcommittee.

The AIEE cable working group sponsored a conference on emergency rating of power cables at the AIEE technical meeting on January 26, 1943, at which nine papers were presented.* Subsequently it seemed desirable to the cable working group to have the AEIC guide also presented for the purpose of giving information to all interested in this country and for stimulating discussion. The information and diversified views in the nine papers were considered in preparing the guide.

This guide is similar in two respects to wartime guides recently prepared by various AIEE committees and subcommittees on various kinds of equipment, namely:

(*a*). It presents principles and assumptions concerning the factors affecting the operation and life of the equipment involved, thereby indicating how to obtain maximum utilization within the prescribed temperature limits.

(*b*). It presents tables of temperatures for occasional emergency use which are above the limits in the AIEE Standards.

Furthermore, this Guide has tables of temperatures for normal operation, that is, day-in and day-out operation, where the temperatures are higher than the limits given in various specifications and standards, including the AIEE Standards. The usage of temperatures such as set forth in the guide for normal operation and for emergency operation will produce sharp increases in rates of deterioration and in rates of failures of cable circuits. Such temperatures are recommended for consideration only as a wartime measure to save critical materials, because their use is otherwise uneconomical as well as damaging to service reliability.

1. Scope

THIS guide has been prepared as an aid in determining reasonable cable operating temperatures for wartime service for both normal and emergency operating conditions. The aim has been to obtain the maximum practical utilization of materials and facilities consistent with maintaining tolerable rates of failure. Tables are presented showing recommended maximum temperatures for representative systems, but the values must be adjusted, in many cases, to take into account the various special conditions that are involved in specific situations. As an aid to the engineer who must make the decision as to the needed adjustments, a large part of the guide is devoted to a discussion of the principles on which the recommendations are based and to suggestions as to the adjustments appropriate for particular cases.

The recommendations cover four types of power cable used mainly in underground systems, namely:

1. Impregnated-paper-insulated lead-covered cable of the solid type.
2. Impregnated-paper-insulated lead-covered cable of the oil-filled type.
3. Varnished-cambric-insulated lead-covered cable.

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* A single pamphlet containing all nine papers is available in limited quantity at Institute headquarters at 75 cents a copy.

Paper 43-104, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943. Manuscript submitted April 21, 1943; made available for printing May 20, 1943.

Personnel of the AEIC subcommittee: Herman Halperin, *chairman;* C. T. Hatcher, and H. W. Collins.

Personnel of the AEIC cable engineering section: W. F. Davidson, *chairman;* G. M. Armbrust, T. J. Brosnan, W. R. Bullard, H. A. Clark, M. T. Crawford, G. E. Dean, R. L. Dodd, F. M. Farmer, C. W. Franklin, T. H. Haines, Herman Halperin, L. I. Komives, C. H. Kraft, S. J. Lisberger, R. E. Morse, H. S. Phelps, F. E. Pinckard, and C. T. Sinclair.

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to one. The larger values of excitation and amplification apply to the larger ratings.

Power amplification and rate of response are interrelated; that is, for a particular design an increase in power amplification will normally result in a reduction of the rate of response, and an increase in the rate of response will normally result in a reduction in power amplification.

Applications and Service Experience

There have been a great many applications of Amplidyne generators in regulating systems, some of which have now been in service from four to five years. Some typical applications were given in a previous article.[1]

The service record of these machines and their circuits has been excellent.

Typical of the type of regulation that can be accomplished is the application of Amplidynes to wind tunnel drives. The arrangement of a wind-tunnel-drive apparatus was described in a previous article.[5] Figure 7 shows the schematic connections of the main drive. In these circuits, the following functions were performed with Amplidynes and associated circuits.

1. Variation of field current of constant speed and variable speed d-c machines—*BD*1, *BD*2, *AD*1, and *AD*2 to

(*a*). Controlled acceleration of the variable speed machines from rest to full speed and matching of the line frequency for synchronizing.

(*b*). Controlled acceleration of the main fan motors from rest

(*c*). Maintain fan motor speed within one-fourth of one per cent.

(*d*). Under any condition impose a definite current limit in the circuit between the d-c machines.

2. Variation of field current of variable speed a-c generator S_2 to

(*a*). Match voltages during the synchronizing period.

(*b*). Maintain system power factor during operation of the tunnel.

Owing to the fact that these circuits involve matching the quantity to be regulated against a standard, it is essential that the proper polarities be predetermined and checked before the apparatus is given full power to regulate. In case of error of connections, the system might cause the value being regulated to depart from the standard with increasing voltage rather than bringing about the desired matching.

References

1. INDUSTRIAL APPLICATIONS OF AMPLIDYNE GENERATORS, D. R. Shoults, M. A. Edwards, F. E. Crever. *General Electric Review*, volume 43, 1940, pages 114–19.

2. THE AMPLIDYNE GENERATOR—A DYNAMO-ELECTRIC AMPLIFIER FOR POWER CONTROL, E. F. W. Alexanderson, M. A. Edwards, K. K. Bowman. *General Electric Review*, volume 43, 1940, pages 104–06.

3. DESIGN CHARACTERISTICS OF AMPLIDYNE GENERATORS, A. Fisher. *General Electric Review*, volume 43, 1940, pages 107–13.

4. STEADY-STATE THEORY OF THE AMPLIDYNE GENERATOR, T. D. Graybeal. AIEE TRANSACTIONS, volume 61, 1942, October section, pages 750–6.

5. LARGE ADJUSTABLE-SPEED WIND-TUNNEL DRIVE, C. C. Clymer. AIEE TRANSACTIONS, volume 61, 1942, March section, pages 156–8.

4. Rubber-insulated cable with and without lead sheath.

This guide is not intended to supersede existing standards and recommended practices for operating temperatures under peacetime conditions of service. Further, it is recognized that if these higher temperatures are used the manufacturer cannot be held responsible under the usual guarantees in standards and specifications for troubles resulting from operation at the increased temperatures.

2. General Principles

Under wartime conditions, the normal balance between many factors that enter into the determination of proper cable operating temperatures is upset. Maintenance of service remains important, but the very high standards of peacetime service may not be justified; economic factors are assigned only small consideration. As a general matter it may be assumed that underground cable systems can operate with failure or trouble rates considerably higher than those reported in the cable operation reports of the Edison Electric Institute without materially affecting the reliability of service to the customers. Some increase in maintenance cost is to be expected, but this is partially offset by the increased use of the investment. Some reduction in life is to be expected; the extent of this is indicated in the assumptions.

Establishment of increased temperatures and increased load ratings under these changed conditions requires a large measure of engineering experience and judgment. However, it is practical to make recommendations as to temperatures for wartime conditions for representative systems and to give sufficient discussion for use in making adjustments in the temperatures as dictated by circumstances peculiar to specific systems.

2.1 Type of System

The recommended temperatures given in Tables I, II, and III apply to representative systems meeting three general conditions. These are:

(*a*). The cable in service was purchased almost entirely under the following standards and specifications: "AEIC Specifications for Impregnated Paper-Insulated, Lead-Covered Cable, Solid Type" (the "Simplified Practice Schedule" issued in 1941 not being included); "AEIC Specifications for Impregnated Paper-Insulated Lead-Covered Cable, Oil-Filled Type"; "AIEE Standard Number 30," issued at various times; "Insulated Power Cable Engineers Association Specifications for Varnished Cambric-Insulated Cables" (the 1939 edition which applies to the recently developed varnished-cambric tapes not being included); the "National Electrical Code," and "ASTM Standard Number 469" for rubber-insulated cable.

(*b*). The system had rates of trouble of the same general order as the national rates of trouble as given in the "Cable Operation Reports" of the Edison Electric Institute. The cable and accessories have no especially bad condition that would lead to a very large increase in troubles with the use of higher temperatures.

(*c*). The power cable systems involved will have reasonably good maintenance, which includes periodic inspections in manholes and repairs when found necessary.

2.2 Temperatures—Definitions

2.21 The temperatures referred to are those at the hottest portion of the cable in a given line or circuit. Where these limiting temperatures exist in only a small portion of the line, attention should, obviously, be given to a determination of the extent to which corrective measures may be justified. (Section 3.8)

2.22 The "normal" temperatures are the maximum copper temperatures that may be reached in regular daily operation during wartime when all of the lines or circuits for a given supply are in service. Where, for example, the daily load cycles for the first five days of the week are practically the same and then the load drops considerably over the week end, then the suggested maximum allowable normal temperature would apply to the conditions that produced the maximum temperature during the week.

2.23 The "emergency" temperatures apply to special operation occurring not more than four periods in any 12 consecutive months for all of the types of cable except the oil-filled type, for which the limiting number of periods is two in any 12 consecutive months. The duration of the special operation is assumed to be a continuous period of time of 24 hours or less for all the types of cable except for the oil-filled type, for which it is assumed that each period is 60 hours or less.

2.24 It is assumed that, both in normal and in emergency operation, the cable is at approximately the maximum temperatures for a few hours or more in each daily lead cycle, which is practically the condition when lines or circuits have their daily heavy-load periods for eight or twelve hours or more.

2.3 Recommended Temperatures

2.31 For impregnated-paper-insulated cables of the solid type the temperatures for normal operation are from 4 to 15 degrees and for emergency operation from 10 to 40 degrees centigrade higher than the temperature limits given in the AEIC Specifications and the AIEE Standards. Lower temperatures are shown in Table I for shielded three-conductor cables than for single-conductor cables, because the maximum temperatures for three-conductor shielded cables are more limited by the effect of cable movement upon the sheath life than by deterioration of the insulation.

2.32 For oil-filled cables the temperatures for normal operation are ten degrees centigrade above the limits given in the AEIC Specifications except for voltages below 26 kv where the increases are smaller, and the proposed emergency temperatures are from 25 to 30 degrees centigrade above the AEIC limits for emergency operation except for voltages below 26 kv.

2.33 For varnished-cambric cables the proposed normal temperatures are from 8 to 14 degrees centigrade above the limits given in AIEE Standard 30, and the proposed emergency temperatures are from 23 to 31 degrees centigrade above those limits.

2.34 For rubber-insulated cables except those with ozone-resistant compounds, the proposed normal temperatures are from 5 to 20 degrees above the limits given in the 1940 National Electrical Code and the proposed emergency temperatures are from 15 to 40 degrees centigrade above those limits. No standard limits are available for ozone-resistant compounds, but general values from manufacturers have been about the same as given in the table.

2.4 Assumed Influence on Cable Life

Higher temperatures result in reduction in cable life. Three assumptions have been made as a basis for the recommendations.

2.41 It is assumed that the wartime "normal" temperatures will at least double the rate of deterioration of the cable as compared with the rate for previous practices based on "normal" temperatures as given in existing standards and specifications. The increase in failure rate will be one indication of an increase in rate of deterioration, but it is to be noted that generally this may not become apparent in less than six months or a year.

2.42 It is assumed that wartime "emergency" temperatures would cause a loss of life, for each period they are used, of less than one per cent of the expected life of the cable and accessories when they were new, and that they would cause no immediate failures.

2.43 The increases in the rate of deterioration for normal operation and in the loss of life of one per cent or less for emergency operation are meant to apply to either the insulation or the sheath as the case may be. It should be recognized that in many cases cable systems have operated at temperatures considerably below the limits given in national standards. In such cases the use of the recommended temperatures in this guide and their accompanying increased temperature ranges may cause increases of 200 or 400 per cent or even more in the rates of deterioration over the rates that previously obtained. The suggested maximum allowable temperatures for wartime operation are based on a large amount of experimental data and operating experience. "Special Considerations" discusses this at some length for a number of cases, while there is more extensive discussion in several papers listed in the bibliography.

3. Special Considerations

3.1 INFLUENCE OF OPERATING VOLTAGE

Temperatures are given in the tables for various nominal operating voltages and were determined by the formulas shown. For other nominal operating voltages, the suggested allowable temperatures can be determined by interpolation from the temperatures in the tables or by the use of the formulas.

3.2 LEAD SHEATH

3.21 For normal temperatures which apply to day-in and day-out operation, one of the main factors affecting the determination of the temperature for lead-sheathed cable is the effect of daily movement of the cable in the manhole incidental to cyclic loading. Such movements may lead to cracking through the sheath before the insulation becomes unserviceable. The temperatures given in the tables are based on the assumption that the daily load varies considerably through each 24-hour period and there is one general movement of the cable in and out of the duct in each 24 hours. If the loading is such as to have two general cycles of movement back and forth, then the temperatures should be reduced in order to avoid considerable cracking through the sheaths in the manholes. On the other hand, if the loading is fairly steady through each 24-hour period, then this factor may permit the use of higher normal temperatures.

3.22 In connection with sheath cracking in manholes incidental to daily cyclic loading, another factor is the training of the cable in the manhole as well as the size of manhole. Where the offset between the center line of the duct and the center line of the line is small, or the bending radii of the cable are small, or the total length between joint wipe and duct mouth is small, then the effect of a given movement of the cable at the duct mouth in causing sheath cracks is increased. Where these characteristics are particularly below what may be considered good practice, then there should be a corresponding reduction made in the allowable temperatures, especially where the daily temperature ranges are large.

3.23 The amount of cable movement at the duct mouth is affected by the length of cable between manholes, within limits. For short lengths of cable, such as 100 or 200 feet, the allowable copper temperatures might be set somewhat higher than given in the tables as far as considerations of the lead sheath are concerned.

3.24 For a given length of cable and temperature range, the amount of movement at the duct mouth is somewhat proportional to the ratio of the total cross section of the copper conductors in the cable to the weight of the cable. For a cable having relatively small conductors, the temperatures might be set at somewhat higher values than given in the tables.

3.25 The strain of the sheath in the manhole incidental to a given movement at the duct mouth is somewhat proportional to the diameter of the cable. Where the cable diameters are considerably more than about one and one half or two inches for low-voltage cables or about two or three inches for high-voltage cables, then the maximum temperatures for normal daily loading might well be less than given in the tables. Where the cable diameters are particularly small, then higher temperatures than given in the tables may be permissible for normal operation.

3.26 Impregnated-paper-insulated cable installed vertically or on steep slopes may be subject to excessive sheath expansion from the internal hydrostatic pressure. The expansion of the lead sheath is accelerated by the rise in temperature of the sheath. For practical purposes it is well to assume that the rate of creep doubles for an increase of about 17 degrees centigrade.

3.27 Where solid-type cables are operated with reservoirs under positive pressure connected to joints, consideration must be given when increasing operating temperatures to the possibility of increased sheath expansion and an increased rate of sheath bursting. Reduction of reservoir pressure may be considered as a compensating measure.

3.28 In locations where corrosion is a problem, increased sheath temperatures may increase the rate of chemical reaction and failures resulting from corrosion.

3.3 CONDUIT

3.31 Another important factor related to the use of higher temperatures for normal operation is the effect of the accompanying increase in conduit temperatures. In some cases the use of the increased temperatures will result in drying out the conduit and surrounding soil with a resultant increase in the heating constant, that is, the thermal resistance between the cable and base ambient, which is unheated earth some distance from the conduit for underground installations. As regards temperatures for emergency operation, the main point on the heating of the conduit is the fact that the tem-

Table I. Maximum Conductor Temperatures for Impregnated Paper-Insulated Lead-Covered Cable for Wartime

Caution: Values in tables are not to be used without full consideration of limitations discussed in text

Nominal Operating Voltage—Kilovolts (Phase-to-Phase)	Maximum Conductor Temperature—Degrees Centigrade	
	Normal Operation	Emergency Operation
Single-Conductor Cable, Solid Type		
0–1	100	125
4	98	121
13	92	113
22	87	105
27	84	100
33	81	95
44	75	84
66	64	70
Three-Conductor Shielded Cable, Solid Type		
13	87	108
22	82	100
27	79	95
33	76	90
Multiple-Conductor Belted Cable, Solid Type		
0–1	98	125
4	94	115
13	85	104
22	76	94
27	71	88
33	65	80
Oil-Filled Type of Cable		
35 and less	85	115
66	85	110
132	80	105

Rules for establishing temperatures shown were as follows:

Type of Cable	Normal Operation	Emergency Operation
Single-conductor, solid	$100-E$(100 degrees centigrade for 0–1 kv and minimum of 64 degrees centigrade)	$125-1.6E$(125 degrees centigrade for 0–1 kv and minimum of 70 degrees centigrade)
Three-conductor shielded, solid	$95-E$	$120-1.6\ E$
Multiple-conductor, belted, solid	$98-E$ (98 degrees centigrade for 0–1 kv)	$120-1.2E$(125 degrees centigrade for 0–1 kv)

E is kilovolts between phase and ground for single-conductor and three-conductor shielded cables and between phases for multiple-conductor belted cables.

perature of the conduit may go up a few degrees for each 24 hours of emergency loading.

3.32 In all calculations as to carrying capacities for a given installation or installations of cable circuits, certain assumptions are made as to the effect of the heating of the cables in raising the temperature of the conduit or air surrounding the cable. If the assumptions are based on factual data obtained by surveys of the conduits and the installations are further followed up by surveys after the circuits are in operation, then the copper temperatures can be calculated with a good degree of accuracy. In such cases the load ratings may be determined without making especially liberal allowances for the temperature rise of the duct or other ambient over the base ambient. If surveys of conduits are made where the cables are operating at relatively high temperatures, then the load ratings can be made somewhat higher than would be the case if no surveys are made. If no factual data are available on the heating characteristics of the medium in which the cable is to be installed and no surveys are to be made, then the temperature that is used in calculating the load ratings might well be five or ten degrees centigrade less than given in the tables. On the other hand, where complete data are available and field surveys are continually made, it may be feasible to use temperatures of a few degrees up to ten degrees centigrade above the limits given in the tables for paper-insulated cables, the larger increases applying to the lower-voltage cables.

3.33 The total carrying capacity of all the cables in a conduit, expressed in kilovolt-amperes, may be increased in some cases by relocating some of the relatively lower-voltage cables into other conduits. Although the increase in current-carrying capacity for the higher-voltage cables might be small, the increase in kilovolt-amperes might be rather large.

3.4 INSULATION

3.41 For, particularly, impregnated-paper-insulated cable made prior to 1920 and having high dielectric losses, and for varnished-cambric-insulated cable used at about nine kv and higher, the existence of large variations in dielectric loss, especially at the higher temperatures, should be recognized in calculating the carrying capacities. Where the information on dielectric losses throughout the expected temperature range of operation for a particular kind of cable is not available, then reasonably large allowances should be made for the dielectric losses or the temperature limits used should be somewhat less than given in the tables for both normal operation and emergency operation. Special attention should be given to the possibility of failures resulting from thermal instability for old cables with high dielectric losses.

3.42 Another effect of the temperature range is in the production of ionization in the solid-type paper-insulated cable. For old belted-type cable such as made in the period of 1920–30 with somewhat less insulation than used prior to 1920, the effect of ionization in normal operation may be considerable in some instances. If the operating experiences have indicated this to be the case, a small reduction below the temperatures given in the table for normal operation should be made, and a somewhat larger reduction should be made in temperatures for emergency operation.

3.43 Cable that has operated at temperatures corresponding to voltages much below its rated voltage may become unfit so far as the insulation is concerned to operate subsequently at rated voltage.

3.44 The insulation of recently made paper-insulated cable of the solid type can withstand somewhat higher temperatures for emergency operation than given in the table.

3.45 Varnished - cambric - insulated cable made in the past few years can withstand temperatures roughly five degrees centigrade higher than given in the table for both normal and emergency operation.

3.46 It should be noted that the resistance to effects of heat by rubber compounds of a given class has been found to vary considerably. If test or operating data are available for a given installation, the data may be used to modify the temperatures in the guide somewhat up or down.

3.47 Deterioration of rubber is greatly accelerated by oxidation. As a consequence, higher permissible temperatures are shown in Table III for lead-sheathed cable than for cables without lead sheath, except for ozone-resistant compounds, where the maximum temperatures are limited by softening of the insulation. Where the ends of lead-sheathed cables are of such construction as to allow air to be more or less in free contact with the rubber insulation, then, excepting ozone-resistant compounds, somewhat lower temperatures than given in the table may be advisable.

Table II. Maximum Conductor Temperatures for Varnished-Cambric-Insulated Lead-Covered Cable for Wartime

Caution: Values in table are not to be used without full consideration of limitations discussed in text

Nominal Operating Voltage—Kilovolts (Phase-to-Phase)	Maximum Conductor Temperatures—Degrees Centigrade: Normal Operation	Maximum Conductor Temperatures—Degrees Centigrade: Emergency Operation
Three-Conductor Shielded and Single-Conductor Cable		
0–1	88	105
4	85	102
13	77	94
22	70	86
27	70	82
Multiple-Conductor Nonshielded Cable		
0–1	88	105
4	82	99
13	70	85

Rules for establishing temperatures shown were as follows:

Normal Operation	Emergency Operation
$88-1.5\ E$ (88 degrees centigrade for 0–1 kv and minimum of 70 degrees centigrade)	$105-1.5\ E$ (105 degrees centigrade for 0–1 kv)

E is kilovolts between phase and ground for single-conductor and three-conductor shielded cables and between phases for multiple-conductor nonshielded cables.

3.5 ACCESSORIES

3.51 In some instances the loading of the cables in emergencies to the temperatures given in the tables will result in movements at the duct mouth of one and one half or two and one half inches or even more. Training of the cable in the manhole should be such as to permit these large movements without buckling of the cable or jamming of the joint against the manhole wall or other joints or undue mechanical strains on connections to the joints to cause serious damage. If this is not feasible, then lower temperatures than given in the tables for emergency operation are suggested.

3.52 In some cases joints and potheads may develop troubles if operated at the emergency conductor temperatures given in the tables. This applies particularly to the higher-voltage cables. In general, however, the accessories on an underground system operate with ambients 15 or 25 degrees centigrade below the maximum duct temperature along the circuit at the time of emergency operation, and so the possibility of trouble with the accessories in such instances is thereby reduced or eliminated. The point, however, is of significance where the cable and accessories are all exposed to the same ambient, as might be the case with an open installation of a circuit in a generating station.

3.53 Where joints are filled with viscous or hard compound, increased tempera-

Table III. Maximum Conductor Temperatures for Rubber-Insulated Cable in Wartime

Caution: Values in table are not to be used without full consideration of limitations discussed in text

Nominal Operating Voltage—Kilovolts	Maximum Conductor Temperature—Degrees Centigrade			
	Normal Operation		Emergency Operation	
	No Lead Sheath	Lead-Sheathed	No Lead Sheath	Lead-Sheathed
Code and Intermediate Compounds				
0–1	60**	70*	70	90*
30 Per Cent Class *AO*, Performance, and Other Similar Compounds				
0–7.5	65	75*	75	95*
Ozone-Resistant Compounds (Oil Base)				
0–7.5	75	75	85	85
7.6–15	70	70	80	80

* Terminal ends of cable assumed to be sealed against entrance of air.

** These temperatures are based on the 1940 National Electrical Code, which gave 50 degrees centigrade for code rubber together with Interim Revision Number 41 dated February 26, 1942, which permitted the use of the higher current ratings given in the previous editions of the code.

tures may cause trouble because of bulging of the casing or because of repeated bulging and collapsing of the casing.

3.54 Reservoirs may be subjected to volume expansion to beyond their limits when cables are operated at some of the higher temperatures given for oil-filled cables, and for those installations of solid-type cable having oil-filled reservoirs connected to the oil-filled joints. Therefore, special provisions should be made to take care of extra expansion of the impregnating compound or oil of the cable where the present reservoirs are inadequate.

3.55 Consideration must be given for oil-filled cable installations to the fact that dropping of heavier loads corresponding to higher temperature limits will result in hydrostatic pressure drops in the cable system greater than contemplated in the original design of the oil supply system. Provision must be made to prevent pressures below atmospheric forming at any point of the line under these conditions.

3.6 LOAD CYCLES

As indicated under "General Principles" the recommended temperatures are based on the assumption that the cable may operate at about these temperatures for a few hours or more in a day. The heavy portion of the load of a given day may be of such short duration that no stable temperature conditions are attained and the maximum temperature is reached only momentarily. In such an event, if the temperature before the time of heavy load is considerably below the permissible temperature in the tables, then the maximum temperature could be five or ten degrees centigrade above the value in the tables, the higher temperatures applying to the lower voltages. This statement applies to temperatures during normal or emergency operation as the case may be.

3.7 INSPECTION

It is recommended that when lines or circuits are operated at temperatures such as given in the tables, the cable systems be given increased inspection. In the case of emergency operation the inspection should be made with little delay.

3.8 SPECIAL MEASURES

As previously indicated, the utilization of lines and circuits already in service can be increased within the allowable temperature limits in this guide through certain practices and arrangements, some of which are listed below:

1. Temperature surveys.
2. Increased inspection and maintenance program.
3. Changing solid bonding to special bonding for single-conductor cables to eliminate sheath losses.
4. Forced cooling by water or air during special temperature peaks at especially hot spots.
5. Elimination of local thermal bottlenecks by
   (*a*). Use of larger conductors.
   (*b*). Change of soil around conduit, as by replacing cinder fill.
   (*c*). Relocating sources of external heat.
   (*d*). Spreading of cables in crowded location.
6. Relocation of one or two cables to permit an increase in kilovolt-ampere loading of the remaining cables above the load carried on the removed cables.
7. Improvements in training of cable in manholes.

References

1. PYROCHEMICAL BEHAVIOR OF CELLULOSE INSULATION, F. M. Clark. AIEE TRANSACTIONS, volume 54, 1935, October section, pages 1088–94.

2. TEMPERATURE LIMITS SET BY OIL AND CELLULOSE INSULATION, C. F. Hill. AIEE TRANSACTIONS, volume 58, 1939, September section, pages 484–7.

3. FACTORS INFLUENCING THE MECHANICAL STRENGTH OF CELLULOSE INSULATION, F. M. Clark. AIEE TRANSACTIONS, volume 60, 1941, July section, pages 778–83.

4. LOAD RATINGS OF CABLE, Herman Halperin. AIEE TRANSACTIONS, volume 58, 1939, October section, pages 535–53.

5. MAXIMUM SAFE OPERATING TEMPERATURE FOR 15-KV PAPER-INSULATED CABLES, C. W. Franklin, E. R. Thomas. AIEE TRANSACTIONS, volume 58, 1939, October section, pages 556–60.

6. TEMPERATURE AGING CHARACTERISTICS OF CLASS A INSULATION, J. J. Smith, J. A. Scott. AIEE TRANSACTIONS, volume 58, 1939, September section, pages 435–42.

7. LOAD RATINGS OF CABLE—II, Herman Halperin. AIEE TRANSACTIONS, volume 61, 1942, pages 930–42.

8. RESULTS OF EMERGENCY OVERLOAD TESTS ON 24-KV 3-CONDUCTOR PAPER AND LEAD CABLE, G. B. McCabe, J. Sticher. *Edison Electric Institute Bulletin*, November–December, 1942, page 405.

9. CALCULATIONS OF ELECTRICAL PROBLEMS OF UNDERGROUND CABLES, D. M. Simmons. *Electric Journal*, May to November, 1932.

Interim Report on Overloading Current-Limiting Reactors

AIEE TRANSFORMER SUBCOMMITTEE

Preface: The present war emergency requires that the maximum use be made of existing equipment and systems and that a minimum of critical material be used for new equipment.

This publication, as well as other guides and reports in this series, has been prepared for the information of users during the war emergency. Upon termination of the war emergency, they will be reconsidered by the Standards committee and the committees that prepared them, and will be approved, revised for normal use, or rescinded.

This procedure is being followed in preference to the preparation of special emergency Standards which might involve redesigning and drastic changes in manufacturing practices. These guides will accomplish the maximum conservation of critical materials, since they provide for the maximum use of existing equipment and systems, as well as new equipment, without changing the fundamental basis on which the present Standards have been prepared.

THIS report presents a general guide for loading current-limiting reactors above rating where specific design and operating data suitable to determine such loading are not available. It should be used in conjunction with the "Interim Report on Guides for Overloading Transformers and Voltage Regulators," presented on June 22, 1942, by the AIEE transformer subcommittee at the summer convention, which appeared in the September 1942 issue of Electrical Engineering, as many data included there are not repeated in this report.

Temperature Limits

The present Standards for reactors provide that the temperature rise of the hottest spot of the windings shall not exceed 65 degrees centigrade for oil-insulated reactors and for dry-type reactors with Class *A* insulation or 90 degrees centigrade for dry-type reactors with Class *B* insulation, and that they shall be suitable for continuous operation at rated load, provided that the ambient temperature does not exceed 40 degrees centigrade and the daily average ambient temperature does not exceed 30 degrees centigrade.

This means that the total temperature of the hottest spot of the windings should not exceed a daily average of 95 degrees centigrade for oil-insulated reactors and dry-type reactors with Class *A* insulation or a daily average of 120 degrees centigrade for dry-type reactors with Class *B* insulation.

Reactors built to the present standards are often installed in compartments with restricted ventilation where the temperature of the reactor may be 10 to 15 degrees centigrade higher than when installed in the open. Under such conditions, dry-type reactors with Class *A* insulation are applied and operated under conditions that may result in an average daily hot-spot temperature of 105 degrees centigrade, and dry-type reactors with Class *B* insulation are applied and operated under conditions that may result in an average daily hot-spot temperature of 135 degrees centigrade.

Because of the aforementioned experience, it is undoubtedly safe to operate at these increased temperatures during this period. In order to prevent confusion in the case of reactors not enclosed in compartments, the ambient temperature is intended to mean the temperature of the air at a sufficient distance from the reactor not affected directly by the losses of the reactor. Likewise, in the case of reactors enclosed in compartments, it is not intended to refer to the temperature within the compartment but to the air surrounding the compartment.

Effect of Ambient Temperature on Continuous Loading for Normal Life Expectancy

The effect of ambient temperature on the loading of reactors is as follows:

If the daily average ambient temperature is below the reference of 30 degrees centigrade ambient, the loading may be increased, and, if the daily average ambient is above the reference ambient, the loading should be decreased as follows:

	Correction Per Cent Per Degree Centigrade Difference From the Reference
For oil-immersed reactors	1.0
For dry-type Class *A* reactors	0.75
For dry-type Class *B* reactors	0.5

Effect of Load Factor on Loading for Normal Life Expectancy

Load Factor

For daily load factors below 100 per cent, the loading may be increased 0.3 per cent for each per cent that the load factor is below 100 per cent, with normal life expectancy. Corrections in greater detail, based on an actual load curve, are not usually justified for normal loading. In no case should the overload permitted by this factor exceed 15 per cent, corresponding to 50 per cent daily load factor.

Paper 43-109, recommended by the AIEE committee on electrical machinery for presentation at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943. Manuscript submitted May 21, 1943; made available for printing June 6, 1943.

Personnel of the transformer subcommittee: M. S. Oldacre, *chairman;* F. S. Brown, E. S. Bundy represented by R. T. Henry, J. E. Clem, I. W. Gross, V. M. Montsinger, J. R. North, W. C. Sealey, F. J. Vogel, C. F. Wagner.

This interim report was prepared by the AIEE transformer subcommittee of the committee on electrical machinery for the purpose of making essential information immediately available to war industries, thus furthering the conservation of valuable material for the war emergency. It is educational and in no way mandatory. It is not intended as a "Standard," and has not been formally approved by the Standards committee nor the board of directors.

Emergency Short-Time Loading for Normal Life Expectancy

The following paragraph and Tables I and II indicate the short-time loads that can be carried without affecting normal life expectancy in 30 degrees centigrade daily average ambient.

Oil-Immersed Reactors

Oil-immersed reactors may be overloaded under emergency conditions in accordance with the curves given in American Standard C57.3, "Guides for Operation of Oil-Immersed Transformers."

Emergency Loading With Moderate Sacrifice of Life Expectancy

Tables III, IV, and V indicate the emergency overloads that can be carried with moderate sacrifice of life, probably not exceeding one per cent for each application, in 30 degrees centigrade daily average ambient.

Use of Other Corrections With Overload Values Given in Tables

Care should be taken that the percentage change from each of the various causes be based on the reactor current rating and not on the increased loading resulting from any of the other corrections.

The effects of the various following factors may be added directly:

For normal operation with normal life expectancy the effects of load factor, ambient temperature, and supplemental cooling may all be added in determining the load capability.

For short-time emergency operation with either normal life expectancy or moderate sacrifice of life, the effect of the actual ambient temperature during the emergency and of supplemental cooling may be added to the value for the emergency operation.

This method of adding corrections is not rigorously correct, but it is sufficiently correct for practical purposes and is simple to apply.

Table I. Dry-Type Class *A* (Enclosed in Compartment)*

Duration of Load (Hours)	Hot-Spot Temperature (Degrees Centigrade)	Times Rated Load Current		
		Following Full Load	Following Three-Quarter Load	Following No Load
1	120	1.21	1.40	1.65
2	115	1.10	1.17	1.27
4	110	1.04	1.06	1.08

* See footnote for Table V.

Table II. Dry-Type Class *B* (Enclosed in Compartment)*

Duration of Load (Hours)	Hot-Spot Temperature (Degrees Centigrade)	Times Rated Load Current		
		Following Full Load	Following Three-Quarter Load	Following No Load
1	150	1.15	1.35	1.60
2	145	1.07	1.15	1.25
4	140	1.03	1.05	1.07

* See footnote for Table V.

Supplemental Cooling

The design of dry-type reactors is such that forced-air cooling may be readily applied, provided sufficient space is available for the cooling equipment. For best results, the flow of air should pass over all the windings of the reactor. The permissible load can be increased in some cases as much as 30 per cent for the same temperature rise. The efficiency of forced-air cooling varies, and each application should be carefully investigated to determine the permissible increased loading.

Table III. Oil-Immersed Reactors

Duration of Load (Hours)	Hot-Spot Temperature (Degrees Centigrade)	Times Rated Load Current	
		Following Full Load	Following No Load
1	137	1.9	2.00
2	130	1.65	2.00
4	125	1.45	1.7
8	120	1.30	1.4
24	110	1.15	1.15

Table IV. Dry-Type Class *A* (Enclosed in Compartment)*

Duration of Load (Hours)	Hot-Spot Temperature (Degrees Centigrade)	Times Rated Load Current		
		Following Full Load	Following Three-Quarter Load	Following No Load
1	137	1.40	1.60	1.80
2	130	1.25	1.30	1.40
4	125	1.15	1.16	1.18
8	120	1.10	1.10	1.10

* See footnote for Table V.

Table V. Dry-Type Class *B* (Enclosed in Compartment)*

Duration of Load (Hours)	Hot-Spot Temperature (Degrees Centigrade)	Times Rated Load Current		
		Following Full Load	Following Three-Quarter Load	Following No Load
1	160	1.25	1.45	1.70
2	155	1.15	1.20	1.3
4	150	1.08	1.10	1.12
8	145	1.05	1.05	1.05

* Dry-type reactors with both Class *A* and Class *B* insulation installed in the open with three or more feet of head room will carry 7.5 per cent more load than when enclosed in compartments. In all cases where these conditions are not met the extra overload should be obtained from the manufacturer.

Cautions and Limitations

It must be recognized that overloads should not be applied to current-limiting reactors without a thorough study of the various limitations involved. If reactors are operating at higher than standard temperatures, the permissible duration of short-circuit may be reduced slightly. Among other limitations are: soldered connections, leads, effects of increased magnetic fields on surrounding equipment, as well as the thermal ability of connected equipment.

Before overloading current-limiting reactors to the full extent covered in this report, it is recommended that overload capabilities of the reactor be checked with the manufacturer.

Design Relationships for D-C Generators for Use in Aircraft

S. R. BERGMAN
ASSOCIATE AIEE

Synopsis: The paper sets forth the different operating conditions met with in aircraft generators as compared with standard types of industrial generators. It discusses certain features inherent in applications to aircraft, such as reliability and light weight. Certain requirements in both electrical and mechanical design are imposed by the fact that a plane is flying through varying altitudes resulting in changing atmospheric conditions, such as rarefication, low temperature, and ionization. Other unusual conditions are created from the motion of the plane setting up accelerating forces on the generator, as well as mechanical forces caused by engine vibrations. Because of important features such as commutation, voltage regulation, cooling, driving means, and light weight, special and unusual methods must be employed. In solving these problems the most perfect methods known in the art of building d-c machines must be resorted to, and the paper lays particular stress on the use of compensation.

Main and Accessory Engine-Driven Generators

THERE are two different methods of driving the generator:

1. The generator is driven from the main engine, in which case it is bolted to the engine and splined to a driving gear inside the engine. In some cases the generator is located in the nacelle and driven through a shafting containing two universal joints and a telescoping shaft so as to take up engine vibrations.

2. The generator is directly connected to an accessory engine. In this case the generator must be designed with relatively high efficiency so as to minimize the weight of gasoline consumed by the accessory engine.

The efficiency of main engine-driven generators runs between 70 and 80 per cent for different sizes (see Figure 3), and those driven by accessory engines should have an efficiency of from 85 to 90 per cent. The limitation of the application to accessory engines lies in the fact that they lose power quite rapidly with altitude since no satisfactory supercharger for this type of engine has as yet been developed.

Weights

Low weight is the natural requirement of all parts in airplanes. The weight of d-c generators for aircraft has been reduced to about one tenth of that of industrial generators of the same rating, and this has been accomplished with only a slight suffering in the efficiency. As an example: A d-c airplane generator rated 30 volts, 300 amperes, 4,400/10,000 rpm weighs 45 pounds—that is, five pounds per kilowatt—and has an efficiency of 77 per cent; an industrial-type generator of the same kilowatt rating at 1,750 rpm weighs about 400 pounds—that is, 45 pounds per kilowatt—and has an efficiency of 80 per cent. However, the airplane generator has at full load a temperature rise of 100 degrees centigrade employing an insulation consisting of glass and mica; whereas the industrial type of generator has a 40 degrees centigrade rise, using mainly cotton as a base insulation.

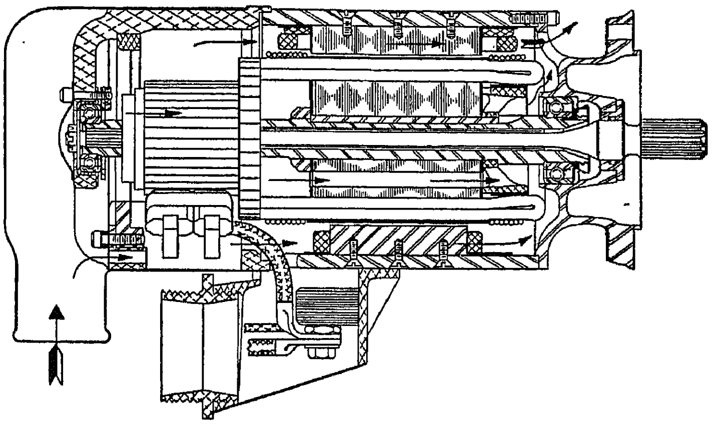

Figure 1. Assembly of d-c aircraft generator showing blast cooling

Future developments will be along the lines of better heat-resisting insulation. Varnishes have already been developed which will stand 200 degrees centigrade continuously without deterioration. Taking advantage of these higher temperatures, we may expect a further reduction in weight.

Influence of Atmospheric Conditions

The atmospheric conditions influence the cooling of the generator, affect the wear of the brushes, and because of rarefication and ionization influence insulation and creepages.

In the region in which the engine-driven generator is installed between the rear of the engine and fire wall, the ambient temperature is rather high, varying from 35 degrees centigrade to 65 degrees centigrade, and remains independent of the altitude. Since the density of the air decreases with the altitude, the cooling decreases in all self-ventilated generators. The permissible output is, therefore, reduced with the altitude and at a certain point, usually within the range of the plane, it becomes zero. Therefore, self-cooled engine-driven d-c generators are not advocated. Another type of cooling system consists of scooping the air from the outside of the plane and forcing this air through the generator. Since this cooling air has a very much lower temperature at high altitudes, it compensates for the decreased density. Investigation shows that the permissible output remains constant independent of the altitude. Blast cooling has, therefore, been generally adopted. In Figure 1 is shown how the ventilation is accomplished in a blast-cooled d-c generator.

The influence of the atmospheric conditions at high altitude on the wear of the brushes is a serious one. While considerable progress has been made in the solution of this problem, it is not as yet fully solved. It is a well-known fact that a good commutator which has operated for some time at sea level builds up a glossy film, the surface of which consists mainly of carbon. At high altitudes, ordinary brushes have been found to wear down quite rapidly. Experiments undertaken in altitude chambers have shown this effect is related to the reduced moisture and oxygen content of the air, but special brush types have been found which give longer brush life. It has also been found that a cool commutator

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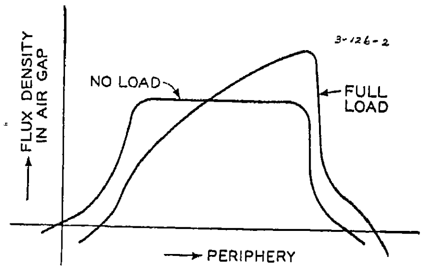

Figure 2. Flux distribution of a noncompensated d-c generator

and brushes are beneficial to brush life. Perfect commutation is important since it contributes to low temperatures. While it is difficult to give any definite rules for the design of commutators and brushes to give satisfactory brush wear, it may be stated that if the temperatures of commutator and brushes are limited to 100 degrees centigrade rise at sea level, and if the current density in the brush contact is also limited to less than 120 amperes per square inch, a reasonable brush life may be expected, provided that proper brushes are used.

The rarefication of the atmosphere at altitude in combination with ionization influences the breakdown voltage of the air. Between small spheres the breakdown voltage is proportional to the air density, for example, at 30,000 feet it is about one third of that at sea level. The ionization within the range of flight is comparatively small, and its influence on the sparkover voltage is probably quite small. However, if an arc is started, then, because of the great mobility of the charged particles, intense ionization will take place, and the arc will extend itself more quickly than at sea level. Therefore, it is quite important that no arcs or sparks are started. This requires three conditions to be fulfilled:

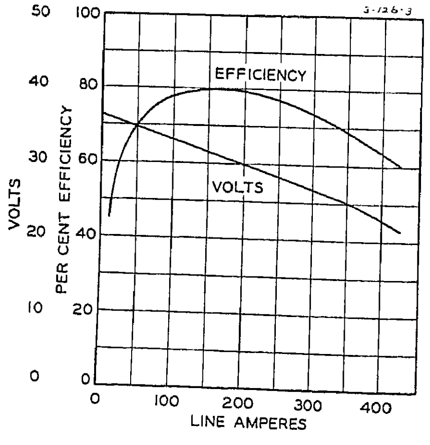

Figure 3. Regulation and efficiency curves of a compensated d-c generator

1. The commutation should be perfect.
2. The brush rig should be so designed as to prevent the sudden lifting of the brushes from vibrations.
3. Sufficient creepage distances should be provided for. For example, in a 30-volt generator the creepage distances should be no less than three sixteenths of an inch.

These circumstances must be taken into consideration when the voltage of the d-c system is determined. The 24-volt system has been found to be quite satisfactory. It has also been found that in certain motor generator sets which are used for turret and gun control 60 volts seems to be satisfactory. Engine-driven generators rated 120 volts have been built and mounted in the nacelle of flying boats; however, the ceiling for these ships is comparatively low, probably from 20,000 to 30,000 feet, and further experience will be required to evaluate fully the merits of voltages higher than the 30-volt system.

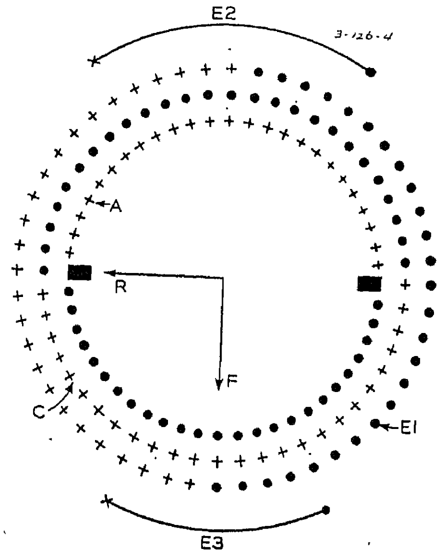

Figure 4. Schematic sketch of compensation and excitation

Vibrations

The vibrations of the engine are very severe, and the magnitude of the forces are not fully known. To guard against failure of the generator, any new design should have an engine test, preferably on the engine for which it has been designed. This run should not be less than 125 hours at different speeds. The engine vibrations are transmitted to the brushes of the generator, tending to throw them off the commutator. To prevent this, spring pressures should be comparatively high, but not so high as to cause excessive brush wear. It has been found that about five pounds per square inch of the brush contact is a good compromise. It is also advantageous to use a large number of small brushes, so that if a few brushes are thrown off, there are others to do the work. The inertia of the brushes should be made as low as possible to minimize these forces.

Another kind of vibration built up by the engine is the torsional vibration transmitted to the shaft. To take care of these vibrations a quill drive is employed (shown in Figure 1). In order to avoid resonance an asbestos packing is used between the inner and outer shafts, which serves as a damping medium. It may be pointed out that this is a nonwearing packing, since it does not slide but depends entirely upon internal molecular friction.

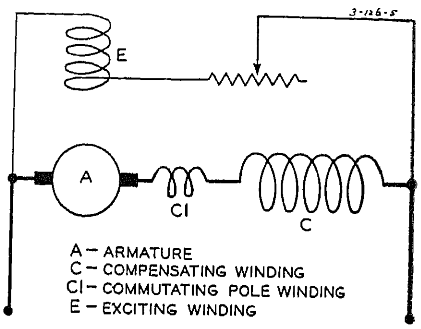

Figure 5. Winding diagram

Electrical Characteristics

The electrical design must secure reliability of operation and light weight.

The reliability requires sparkless commutation at all speeds and for short heavy overloads; even short circuits should not be injurious to the commutator or brushes. In Figure 2 is shown a typical flux distribution curve in a noncompensated generator. It may be noted that

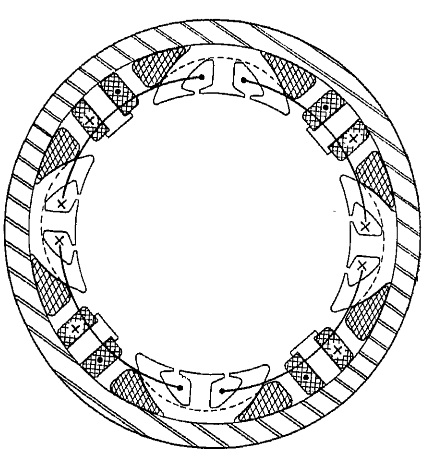

Figure 6. Structure of d-c generator with salient poles and compensation

at load the flux has been distorted in height and also shifted at the same time. This flux distortion is reflected in a similar distortion of the potential around the commutator, resulting in an increased maximum voltage between bars and also a piling up of the voltage toward one of the brushes. Under extreme conditions the maximum volts between bars may be increased from 30 to 40 per cent at full load, and near the brush it will be multiplied several times. Should excessive sparking occur, particularly when the air is rarefied and ionized, an arc may be formed and carried around to the next brush, very likely ruining the generator. This phenomenon is eliminated by the use of compensation, which has for its main object the stopping of flux distortion.

Since the heating is the limiting condition for light weight, an electrical design should be adopted which gives minimum losses. One solution which meets this requirement is the use of compensation, as it eliminates the load losses. Since compensation neutralizes the armature reaction, no field distortion takes place; hence, no extra core loss occurs. Compensation also eliminates sparking and thus does away with extra losses in the brush contacts and in the short-circuited coils. The extra losses in generators without compensation may be considerable, particularly in high-speed machines at weak field. In extreme cases it has been observed to amount to eight points in the efficiency.

Another feature of reliability of the generator is the proper shape of the regulation curve, showing the relationship between amperes load and the terminal voltage. As shown in Figure 3 this curve should be a uniformly drooping line, which is helpful in giving stable operation of the voltage regulator and also contributes to equal current distribution when several generators are operated in parallel, as is the case in multiengined planes. In Figure 3 is shown a typical regulation curve of a compensated generator, showing its uniformly drooping character.

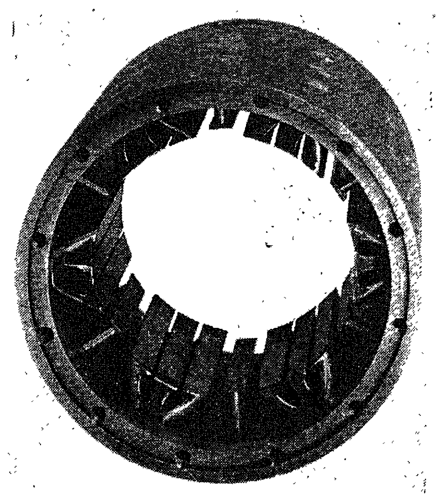

Figure 8. Six-pole field structure of compensated d-c generator

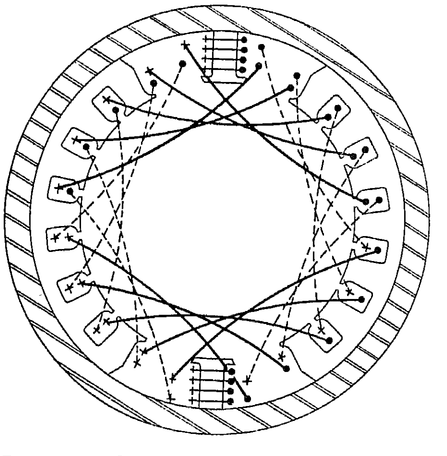

Figure 7. Structure of d-c generator with distributed windings

Compensation

The principle of compensation is shown schematically in Figures 4 and 5. Referring to Figures 4, *A* represents the armature winding which may be wound with any of the usual types of windings. Surrounding the armature is a field structure carrying the compensating winding *C*, which is connected in series with the armature, in opposition to it and equally as strong as the armature reaction *R*; thus these two windings constitute a nearly noninductive pair; that is, no flux except a small leakage flux will be produced. In order to commutate, a certain amount of commutating flux is necessary, and this flux is produced by overcompensation located on commutating poles, which for simplicity is not shown in Figure 4.

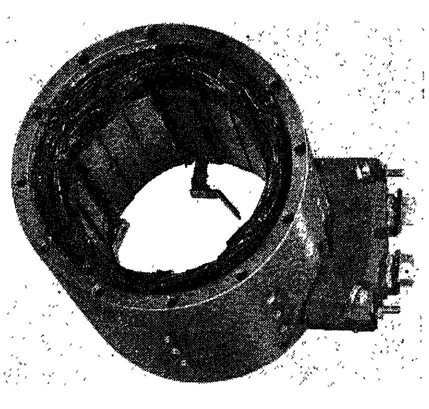

Figure 9. Complete field of four-pole compensated d-c generator

The field excitation *F* may be applied in two different ways:

1. In concentrated coils *E*2 and *E*3 assembled on salient poles.
2. It may be distributed in a winding *E*1 in which case we arrive at a d-c generator in which both the excitation and the compensation are fully distributed.

In Figure 6 is shown the structure of a d-c generator with salient poles and concentrated excitation. It also shows the compensating winding and the commutating poles with their windings.

In Figure 7 is shown the structure of a d-c generator with both the compensation and excitation distributed. Full drawn lines indicate the compensation, and dotted lines the excitation. It also shows the commutating poles with their windings. When inspecting the winding in Figure 7, it is of interest to observe that these windings are similar to a two-phase winding in a two-phase induction motor, one phase corresponding to the excitation and the other phase corresponding to the compensation. These two windings are 90 electrical degrees apart in space. It should also be noted that the pitch of these windings is 50 per cent, which secures short end windings and thus reduces losses.

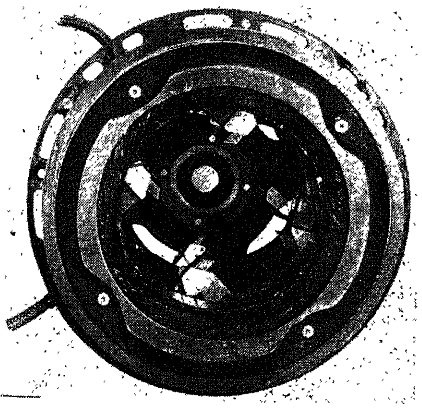

Figure 10. Complete field of an accessory engine-driven d-c generator with fully distributed windings

In order to further illustrate the principle of compensation, reference is made to Figure 8, which is a photograph of a six-pole field structure rated 30 volts, 200 amperes, 2,500/5,000 rpm.

Figure 9 shows a photograph of a four-pole structure with four salient poles rated 30 volts, 300 amperes, 4,400/10,000 rpm

Figure 10 shows a photograph of a complete field with windings and brush holders of an eight-kw accessory engine-driven generator with fully distributed windings

Frequency Modulation for Power-Line Carrier Current

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ASSOCIATE AIEE

FREQUENCY modulation is known to most people today as a system of broadcasting that is remarkably free from the effects of interference and is capable of providing high fidelity reception. These advantages have been realized largely by employing frequencies of 40 megacycles and higher which are well above the present broadcast band. A great amount of effort has been devoted to this field, and therefore, frequency modulation has been well analyzed both in theory[1-3] and experiment.[4,5] However, most of the work has been done with systems using a large deviation ratio which is usually defined as the ratio of the carrier deviation from normal to the highest modulating frequency.

This paper presents results of investigations carried on with a frequency-modulated system for power-line communication, employing frequencies below 200 kilocycles and a deviation ratio of one to one. In this way, the same band width as required for amplitude modulation, or *A-M*, can be utilized. The results of the investigation substantiate the work of others[9] and indicate that even with a one-to-one deviation ratio an *F-M* system is less disturbed by interference than a comparable *A-M* system. In addition, when such a system is subjected to the type of noise encountered on some power lines, the difference between *A-M* and *F-M* is still more prominent.

Outline of Theory

Unfortunately frequency modulation is not as widely understood as amplitude modulation. If *F-M* had been introduced and used for a number of years before *A-M*, the reverse would probably have been true. Both *F-M* and *A-M* are descriptive of the manner in which a modulating quantity alters a sine function that represents a carrier frequency. Since a carrier frequency is an alternating quantity having both amplitude and frequency as its principal characteristics, it is possible to operate on either one of these to cause the carrier frequency to carry intelligence.

One effective way to compare two different modulation schemes is by the equations for the modulated signal. The equation for an *A-M* signal is merely a sine function whose amplitude, A, is varied in some fashion and is usually expressed as

$$e=A(1+m\sin\alpha t)\sin\omega t \qquad (1)$$

where

$\omega=2\pi$ times the carrier frequency
$\alpha=2\pi$ times the modulating frequency
$m=$ amplitude of the modulating frequency and is one for 100 per cent modulation

This equation can be expanded by trigonometric identities into three separate sine functions

$$e=A\sin\omega t+\frac{mA}{2}\cos(\omega-\alpha)t-\frac{mA}{2}\times\cos(\omega+\alpha)t \qquad (2)$$

which can be used to determine the band width of various type channels.

In a similar manner, the equations for a frequency-modulated signal can be developed. Since this has often been done in the literature,[1,3] the important steps only will be given. The equation for the modulated signal is

$$e=A\sin\left(\omega t+\frac{\Delta\omega}{\alpha}\sin\ \alpha t\right) \qquad (3)$$

To grasp the significance of this equation, it is only necessary to remember that $\Delta\omega$ is directly proportional to the amplitude of the modulating frequency. As the amplitude of a sine function is varied in equation 1 for an *A-M* signal, the frequency of a sine function is varied in equation 3 for a frequency-modulated signal. This last equation can be expanded by means of identities into

$$\begin{aligned}e=A\Big\{&J_0\left(\frac{\Delta\omega}{\alpha}\right)\sin\omega t+\\&J_1\left(\frac{\Delta\omega}{\alpha}\right)[\sin(\omega+\alpha)t-\sin(\omega-\alpha)t]+\\&J_2\left(\frac{\Delta\omega}{\alpha}\right)[\sin(\omega+2\alpha)t+\sin(\omega-2\alpha)t]+\\&\cdots\cdots\cdots\cdots\cdots\cdots+\\&J_n\left(\frac{\Delta\omega}{\alpha}\right)[\sin(\omega+n\alpha)t+(-1)^n\sin(\omega-n\alpha)t]\Big\}\end{aligned} \qquad (4)$$

where $J_n\left(\frac{\Delta\omega}{\alpha}\right)$ is the nth order Bessel function of the first kind.

Therefore, instead of one upper and one lower side band as in equation 2, the possibility of many side bands exist. The magnitude of these side bands are fixed by the Bessel functions $J_0\left(\frac{\Delta\omega}{\alpha}\right)$ to $J_n\left(\frac{\Delta\omega}{\alpha}\right)$. However, if $\frac{\Delta\omega}{\alpha}\leq 1$, only the first two terms are significant, and the values of the Bessel functions of the second to nth order

$$\left[J_2\left(\frac{\Delta\omega}{\alpha}\right)\text{to } J_n\left(\frac{\Delta\omega}{\alpha}\right)\right]$$

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rated 30 volts, eight kw, 4,000 rpm.

Both the salient pole construction and the field using distributed windings are being used for aircraft generators. It has been found that the salient pole structure is the lightest of the two structures by about 10 per cent. Since weight is of paramount importance, the salient pole construction is now given preference. While both methods lead to very good commutation, the distributed windings seem to be the most powerful means of obtaining perfect operation, the reason being that it minimizes the leakage and secures a very clean neutral. As an example of how powerful the combination of a distributed compensation and excitation winding is, the author built a separately excited d-c generator rated 150 kw, 15,000 volts, having an average volts per bar of 90. which at full voltage withstood dead short circuits with only a minor flicker on the commutator.

Large industrial machines which have to meet severe operating conditions have been made for many years with compensation. In this paper it has been proved that small d-c generators of the compensated type can be made in a very compact form with light weight.

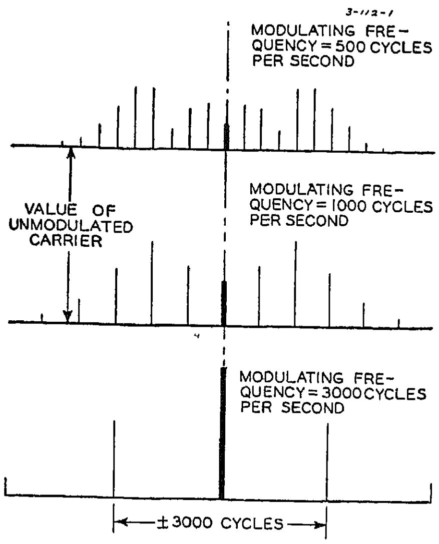

Figure 1. Chart of the amplitudes of the side bands for three different modulating frequencies when the deviation ratio is one to one

are small. Advantage is taken of this fact in the system discussed in this paper.

Another type of modulation has been used that is closely related to *F-M*, namely, phase modulation or *P-M*.[6] Actually, a frequency shift always changes the rate of phase change and vice versa so that the two functions are dependent on each other. By definition, the two types of modulation, *F-M* and *P-M*, have become associated with the nature of the modulated signal. The equation for a phase-modulated signal, comparable to equation 3, is

$$e=A\sin(\omega t+\phi\sin\alpha t) \qquad (5)$$

This equation can be expanded to be the same as equation 4 except with ϕ substituted for $\frac{\Delta\omega}{\alpha}$. Both of these terms have the dimension of phase angle. For the same intensity of all modulating frequencies, in phase modulation the phase shift is constant, but the frequency shift is proportional to the modulating frequency; while in frequency modulation, the frequency shift is constant for all modulating frequencies and the phase shift is inversely proportional to the modulating frequency. Also, the band width of the former is approximately proportional to the modulating frequency, and the band width of the latter is practically constant. Sufficient swing or modulation cannot be obtained easily with phase modulation, and, since reactance tubes can produce enough frequency modulation at low carrier frequencies, strictly frequency modulation was used in the investigation reported here.

Deviation Ratio

The choice of a one-to-one deviation ratio was influenced principally by the desire to restrict the band width of individual channels to the same space that would be required for a comparable *A-M* channel. This is quite important from the standpoint of any user of power-line carrier. The art has progressed to such a stage that many pieces of auxiliary apparatus have been designed and standardized around amplitude-modulated systems. Therefore, existing apparatus as well as equipment already in production could be used with an *F-M* transmitter and receiver to set up a channel. In other words, if the band-width characteristics of line traps and tuning units are applicable to *F-M*, then there would be no change in the application and installation practices already in use.

Even with a one-to-one deviation ratio, the frequency shift is a large percentage of the carrier as compared to the per cent frequency shift used in present day *F-M* broadcast practice. For instance, a 3,000-cycle shift is six per cent of a carrier frequency of 50 kilocycles, whereas the standard 75-kilocycle shift of a broadcast transmitter at 40 megacycles is only about 0.2 per cent. A greater deviation could be generated at these low frequencies, but the band-width requirements of the receiver and coupling apparatus would be excessive. On the other hand, a smaller deviation would not reduce the required band width proportionately and would only result in a lower degree of modulation.

A better picture of what a one-to-one deviation ratio produces in the way of side bands is given in Figure 1, which shows a chart of the amplitudes of the various side bands for different modulating frequencies. This chart was made up by plotting the amplitudes of the various terms given in equation 4, in which $\Delta\omega$ corresponds to a frequency of 3,000 cycles in all cases. From this diagram it can be seen that the bulk of the side bands are confined to a band width of ±3,000 cycles. Of course, if the utmost in fidelity is desired, some of the side bands extending beyond ±3,000 cycles would have to be transmitted and received. Actually, if all the side bands outside of the ±3,000 cycle band were eliminated, only about four per cent of the total energy in the modulated wave would be lost. This would cause a small amplitude modulation of the carrier to appear, but the limiter in the receiver restores the carrier to a constant amplitude. However, the distortion resulting from eliminating the higher side bands is small compared to the total distortion in any *A-M* carrier channel.

Noise Considerations

Noise reduction by means of frequency modulation has been the largest factor in promoting broadcast *F-M*. Interference to the reception of a desired signal may come from many sources and be of various types. In space radio, the most uncontrollable interference is that which takes the form of modulated frequencies in the band accepted by the receiver. The energy in these frequencies is usually uniformly distributed over the band of any channel and is often termed external random noise. With varying magnitudes of this type noise, *F-M* has the characteristic of keeping the interference well rejected until the magnitude of the noise bears a certain ratio with respect to the desired signal. Then the noise is accepted, and the desired signal is rejected. In wide-swing or large-deviation *F-M* this threshold is quite abrupt and occurs when the signal-to-noise ratio becomes about two. It has been demonstrated[3] that in

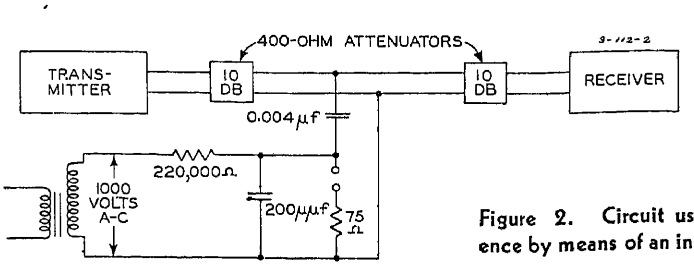

Figure 2. Circuit used to produce interference by means of an intermittent arcing air gap

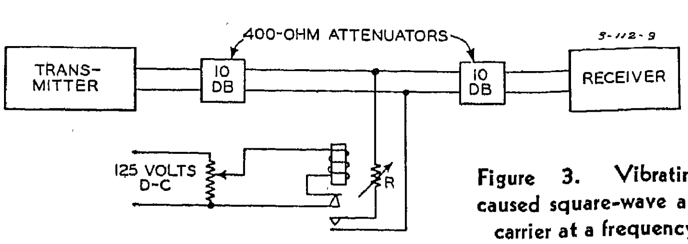

Figure 3. Vibrating-relay circuit which caused square-wave amplitude modulation of carrier at a frequency of about 100 cycles

a small-deviation system, this threshold is not as abrupt and occurs at a lower signal-to-noise ratio. Therefore, it develops that a small-deviation system can deliver usable intelligence from a signal whose signal-to-noise ratio is less than two. This is a definite advantage when the primary function of a service is to get intelligence from one point to another.

There always exists on power lines a fairly high level of random noise caused by corona and arcs of various types acting as sources of amplitude-modulated radio frequencies. In these cases more transmitter power helps in improving the signal-to-noise ratio. Noise of this nature when added to the desired carrier causes a complex type of amplitude variation and phase shift. This produces a signal which is similar to a carrier and a single side band. It is possible for the limiter in an *F-M* receiver to remove the amplitude modulation, but of course it is impossible to eliminate the phase shift. Therefore, it is this phase shift that is the source of noise for an *F-M* channel. It is possible in an *A-M* system for noise to cause a 100 per cent modulation of the carrier, and this is also the limit to the degree of modulation by the desired signal. In frequency modulation it is impossible for noise to cause a phase shift of more than about one radian, but it is possible for the desired signal to cause a much greater phase shift. The phase shift is one radian for the highest value of α when a one-to-one deviation ratio is used. This phase shift becomes greater as α decreases and is the reason for more noise interference present at the higher modulating frequencies. By assuming a random-type noise, the noise reduction theoretically possible by means of *F-M* over *A-M* is six decibels. This is computed from the fact that noise in *A-M* is the same over the audio spectrum while in *F-M* the noise is zero at zero audio frequency and increases linearily to the same value as in *A-M* at the highest audio frequency. Hence, when the noise is integrated over the audio range, it is just half as great in *F-M* as in *A-M*.

Random noise may not be the only type of interference encountered in carrier current or any system where metallic circuits of one kind or another are used for the medium of transmission. In fact, another type of interference, apparently caused by corona, has also been observed on power lines. The carrier in traversing the power line becomes amplitude modulated with noise, and for lack of a better name this phenomenon has been called "corona modulation." The modulation is apparently a result of corona producing an impedance change to the flow of the carrier-current signal. If only this type of noise is present, an increase in transmitter power to surmount the noise does not help because the per cent modulation from the noise stays the same. This modulation is a function of the total attenuation between the transmitter and the receiver and the amount of corona present on the lines. A simple experiment that can be performed to detect this type of noise is to listen to a signal in a carrier receiver with no automatic volume control in operation. When the transmitter is turned off, this type of noise will decrease or completely disappear, indicating that it comes in as modulation of the carrier signal. In this case, the noise is carried by symmetrical side bands about the carrier as represented by the last two terms of equation 2. However, when this type of noise is fed into an *F-M* receiver which contains a balanced discriminator, these side bands are balanced out, since a discriminator is a slope filter whose output is proportional to the frequency deviation from normal. Because an *A-M* signal has amplitude variations but no phase or frequency variations, the output of the discriminator in this case would be zero. Herein is the outstanding advantage of frequency modulation for power-line carrier because it provides the only real solution to corona-modulation noise.

In an effort to improve the discrimination against interference in an *F-M* system still further, advantage may be taken of the energy versus frequency characteristic of the voice.[11] This characteristic indicates that the bulk of the energy in voice exists in the low audio frequencies. Therefore, it is possible to pre-emphasize the higher audio frequencies in the transmitter without exceeding the modulation capabilities of the transmitter. This, of course, makes it necessary to use a de-emphasis circuit in the receiver. Then the scheme is in effect a combination of phase modulation and frequency modulation. It provides a means to get the greatest frequency shift for any modulating frequency without exceeding the band width of any particular channel.

Noise Measurements

With these known facts, a preliminary setup was made in the laboratory consisting of one transmitter and one receiver. The functioning of various circuits was checked, as well as the characteristics of *F-M* when noise was present. These initial results were so promising that a complete communication channel was later set up in the laboratory which involved two transmitters and two receivers and some auxiliary equipment with which more elaborate measurements were made. After considerable development work, a third group of samples was built which embodied all of the improvements and circuit designs worked out in the previous samples. The final samples involved commonly used *F-M* circuits such as a pre-emphasized frequency modulator, a modulation limiter, and a class *C* amplifier in the transmitter; with a radio-frequency amplifier, a limiter, a discriminator, a de-emphasized audio amplifier and a carrier-off noise suppressor in the receiver. Naturally, certain modulations and improvements were made in order to adapt these *F-M* circuits to low-frequency operation.

Extensive tests were made with the samples, of which the noise-reduction measurements were the most interesting. Various schemes were used to produce noise, but the two that proved most acceptable are shown in Figures 2 and 3. The circuit shown in Figure 2 introduced a type of noise which was principally characterized by an arc as a source of radio-frequency energy. The circuit in Figure 3 amplitude-modulated the carrier with a square wave at a rate of about 100 cycles so that several simultaneous noise side bands were produced. Measurements were made to indicate differences between an *F-M* channel and *A-M* channel having the same transmitter power, band width, audio characteristic and attenuation between transmitter and receiver. These measurements were made after both systems were set up in such a way that a 500-cycle modulating frequency produced the same output from both receivers and caused 100 per cent modulation of the *A-M* transmitter and a deviation of 3,000 cycles in the *F-M* transmitter. This particular audio frequency was used because it was just below the point in frequency where the pre-emphasis and de-emphasis circuits began to function. Table I gives the amount the noise level in *F-M* was found below the noise level in *A-M* in decibels. The results in Table I are an average of several measurements with each of the samples mentioned.

Table I

Type of Noise	Unemphasized F-M	Pre-emphasized F-M
Figure 2	11	16 to 19
Figure 3	19	23 to 25

The results show considerably more noise reduction than theory predicted. The theory is based on the assumption that the noise is random, while circuits used here did not necessarily produce random noise. It is felt that the noise encountered on power lines is similar to some combination of the two types of noise employed in these tests. It may be all of one type or any combination of the two. The noise reduction is greater than expected, principally since the bulk of the energy in the noise is in the lower audio frequencies.

Based on these noise measurements made with the arc-noise generator in Figure 2, which utilizes the less favorable type of noise, it would take the following transmitter powers to give the same signal-to-noise ratio in a receiver under the same conditions.

System	Watts
AM	100
Unemphasized *FM*	8
Pre-emphasized *FM*	2

This indicates the possibility of greater transmission ranges with medium power levels which may allow some communication problems to be solved without repeating stations.

Other Advantages

Noise reduction is not the only achievement of frequency modulation as far as communication for power-line carrier is concerned. There are other characteristics which can be utilized to advantage in the various types of communication. The presence of a limiter in the *F-M* receiver is analogous to a very fast and flat type of automatic volume control. The automatic volume control used in *A-M* receivers is of necessity reasonably slow and usually not very flat. One type of communication wherein this characteristic is advantageous is the two-frequency duplex where one frequency is used to transmit in one direction and the other frequency is used to transmit in the opposite direction. The transmitter and receiver audio circuits must then connect to a hybrid circuit and can be set closer to the critical point if the audio output of the receiver is quite constant. Since the limiter in an *F-M* receiver can hold the output extremely constant, a two-frequency voice channel can be operated with higher audio gains, from terminal to terminal.

The other type of communication commonly used is the single frequency duplex or automatic simplex where the voice starts the transmitter and blocks the receiver on outgoing speech. To reduce the amount of clipping of words, it is desirable to accomplish the switching of the transmitter and receiver in as short a time as possible. One inherent limitation in an *A-M* system for fast switching of a channel is the surge of rectified carrier in the detector of a receiver when the transmitter starts. For smooth operation it is necessary to hold the receiver in an inoperative condition until this surge dies down. This difficulty cannot be completely overcome with an *A-M* system because turning the transmitter on and off is fundamentally amplitude modulation, and the detector is designed to detect amplitude modulation. When *F-M* is used, the detector in the receiver is insensitive to amplitude variations and is only sensitive to any phase variations that may come about in starting and stopping the transmitter. Since any phase shifts are small compared to the phase shift caused by modulation by the desired signal, this surge is greatly minimized in the receiver when *F-M* is used.

The transients in an automatic-volume-control circuit of an *A-M* receiver at the moment carrier is received prevents the possibility of opening the receiver quickly and getting an undistorted signal in the output. These transients in automatic-volume-control circuits change the gain of the receiver and hence the output of the receiver is modulated by these transients which causes distortion. On the other hand, a limiter in an *F-M* receiver is practically instantaneous in action, and the receiver can be opened very rapidly without the possibility of the receiver output becoming distorted by transients.

It has been shown[12,13] that two broadcast *F-M* stations operating on the same frequency have considerably more interference-free operating area than two *A-M* broadcast stations on the same frequency. This would still be true of two *F-M* power-line carrier communicaton channels, except to a lesser degree, since the deviation ratio is one to one instead of five to one. With the rapid installation of carrier-current apparatus throughout the country, there are many places where frequency congestion is becoming a problem. One partial solution to this problem would be by means of *F-M*, since a certain frequency could be repeated throughout a system more often than with amplitude modulation.

This paper has largely dealt with aspects of frequency modulation as applied to voice communication. Obviously, there are certain other services where *F-M* is much better than *A-M*. It has been demonstrated that a frequency-shift-type signaling channel[10] is superior in most all respects to a keyed-carrier type of signal channel. It is much easier to transmit frequency or phase accurately through nonlinear tube circuits than it is to transmit an amplitude faithfully through these same type circuits. Therefore, frequency modulation seems basically advantageous when tube circuits are involved. Also, all radio-frequency circuits associated with an amplitude-modulated signal must be linear whereas they need not be linear with a frequency-modulated signal.

Conclusion

It seems certain that the characteristics of *F-M* make it desirable for many applications in the carrier-current field. The outstanding characteristic of *F-M* is the rejection of the interference resulting from undesired amplitude modulation of the carrier.

The chief advantages for *F-M* when applied to power-lines may be summarized as follows:

1. Capability of noise reduction.
2. Possibility of longer channels or less power in transmitters.
3. Less interference between channels on the same frequency.
4. Flat automatic volume control in receiver.
5. Fast switching of channel possible in single frequency voice communication.
6. Same auxiliary apparatus as used for *A-M* channels because of the same band-width requirements.

These advantages have been discussed to a considerable extent in the body of the paper.

It should be pointed out that *F-M* equipment cannot be applied to operate with existing *A-M* equipment. A signal from an *F-M* transmitter can be reproduced by an *A-M* receiver by tuning the receiver to one side of the carrier[8] so that the selectivity characteristic performs similar to a slope filter. However, this is not acceptable for general application because of the distortion resulting from the nonlinearity and narrowness of the sloping part of the characteristic. Also, any frequency drift which may occur in the *F-M* carrier results in excessive distortion in the *A-M* receiver because the slope filter which is obtained by the one-

The Calculation of Unbalanced Magnetic Pull in Synchronous and Induction Motors

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Synopsis: Unbalanced magnetic pull may be defined as the net sideways force between the stator and rotor of an electric machine resulting from a difference in the air-gap flux densities on opposite sides of the machine. This difference in flux density is, in general, caused by a difference in the air gaps on the two sides.

There is outlined herein a method of calculation of unbalanced pull which takes into account the combined effects of saturation, parallels, and primary reactance. The increased accuracy which this method affords should make possible more accurate predictions of mechanical deflections and critical speeds.

I. Induction Motors

INDUCTION motors may operate at any speed from zero to synchronous speed and at any applied voltage from zero to 110 per cent of normal. Operation of the machine under these conditions may be illustrated on the no-load saturation curve. The no-load saturation curve will be represented by a power equation of the form:

$$i_m=eI_g+e^mI_s$$

Assume that the flux density over a certain section of the air gap of a machine is uniform and that a uniform change is made in the length of the air gap over this section while the magnetizing current is maintained at a fixed value. It is shown in Appendix I that the resulting change in gap density is given by the equation:

$$b=\frac{\left(1-\frac{K_c'}{K_c}\,\frac{g-\Delta}{g}\right)eB_g}{\frac{K_c'}{K_c}\,\frac{g-\Delta}{g}+me^{m-1}\frac{I_s}{I_g}}$$

This equation may be used to give the rise in gap density caused by a given change in the air gap for an induction motor with a series or two-parallel primary winding operating at any speed and voltage. However, it should be noted that the no-load saturation curve does not accurately represent the flux conditions in the motor under load. Use of this curve is justified, however, by the fact that the total saturation will normally be divided fairly evenly between the stator and the rotor, and the density rise as calculated by the foregoing equation will be nearly correct.

If a very small deflection is considered, this equation has the following form:

$$b=\frac{\frac{\Delta}{g}eB_g}{1+me^{m-1}\frac{I_s}{I_g}}$$

This equation may be written in the form:

$$b=K_s\frac{\Delta}{g}eB_g$$

where

$$K_s=\frac{1}{1+me^{m-1}\frac{I_s}{I_g}}$$

and this represents the effect of saturation on the flux density rise.

A. UNBALANCED PULL FOR A SERIES OR TWO-PARALLEL PRIMARY WINDING

Consider now a machine having constant flux density around the periphery in which the rotor is displaced to one side a small amount.

The deflection will then be approximately sinusoidal around the periphery, and the flux density change will also be approximately sinusoidal, since $b=\text{constant}\times\Delta$.

The fundamental formula for magnetic pull is:

$$\text{Magnetic pull}=0.01386B^2A$$

Using this formula together with the equation for density rise, it is easily proved that:

$$\text{Unbalanced magnetic pull}=0.01386K_s\frac{\Delta}{g}e^2B_g^2A$$

In another form, this may be written:

$$\text{Unbalanced magnetic pull}=\text{constant}\times\frac{e^2}{1+me^{m-1}I_s}$$

Solving for a maximum:

$$E^{m-1}=\frac{2}{(m^2-3m)\frac{I_s}{I_g}}$$

where E is the per unit voltage at which maximum pull occurs.

This formula gives the voltage at which maximum pull would occur if the gap density were uniform around the periphery of the machine. However, with a sinusoidal wave form and with part of the saturation in the teeth it is apparent that maximum pull for the entire wave will not occur when the peak density reaches the aforementioned value for maximum pull but will occur at some higher density. For an unsaturated machine, when the peak density reaches the aforementioned value, the average pull

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sided tuning of an A-M receiver is very narrow.

References

1. Amplitude, Phase, and Frequency Modulation, H. Roder. Institute of Radio Engineers *Proceedings*, volume 19, December 1931, page 2145.

2. Frequency Modulation, S. W. Seeley. Radio Corporation of America *Review*, volume 5, April 1941.

3. Frequency Modulation, W. L. Everitt. AIEE Transactions, volume 59, 1940, November section, page 613–25.

4. A Method of Reducing Disturbances in Radio Signaling by a System of Frequency Modulation, E. H. Armstrong. Institute of Radio Engineers *Proceedings*, volume 24, May 1936, page 689.

5. Frequency Modulation (a book), August Hund. McGraw-Hill Book Company, Inc., New York, N. Y., 1942.

6. Frequency Versus Phase Modulation, Herbert J. Scott. *Communications*, August 1940.

7. Band Width and Readability in Frequency Modulation, M. G. Crosby. Radio Corporation of America *Review*, volume 5, January 1941.

8. *F-M* Carrier-Current Telephony, Braulio Dueno. *Electronics*, May 1942, page 57.

9. Narrow Band Versus Wide Band in *F-M* Reception, M. L. Levy. *Electronics*, June 1940, page 26.

10. Frequency-Modulated Carrier Telegraph System, F. B. Bramhall, J. E. Boughtwood. AIEE Transactions, volume 61, 1942, January section, pages 36–9.

11. Telephone Theory and Practice (a book), K. P. Miller. McGraw-Hill Book Company, Inc., New York, N. Y., 1930.

12. Field Tests of Frequency and Amplitude Modulation with Ultrahigh-Frequency Waves, Part I, I. R. Weir. *General Electric Review*, volume 42, May 1939, page 188.

13. Field Tests of Frequency and Amplitude Modulation with Ultrahigh-Frequency Waves, Part II, I. R. Weir. *General Electric Review*, volume 42, June 1939, page 270.

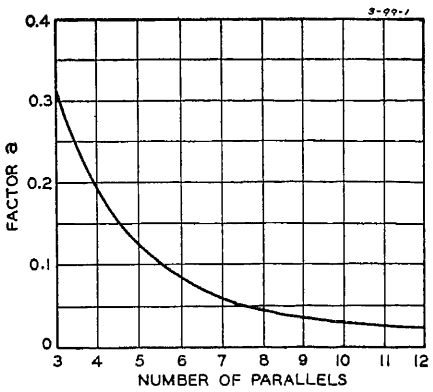


Figure 1. Factor a plotted against the number of parallels

will be one half the pull that would be obtained if the density were uniform at this value. However, with saturation, it is a close approximation to take this value as two thirds of the pull calculated with a uniform density. There is an important exception, however; that is, that, when the maximum pull comes at higher than 110 per cent voltage, the unsaturated value of one half should be used.

This gives two cases as follows:

1. For E less than 1.1

Maximum unbalanced pull =

$$2.88K_sE^2\frac{B_g{}^2A}{g}\times10^{-4}$$

2. For E greater than 1.1

Maximum unbalanced pull =

$$2.62K_s\frac{B_g{}^2A}{g}\times10^{-4}$$

Both formulas apply to a deflection of $^1/_{32}$ inch.

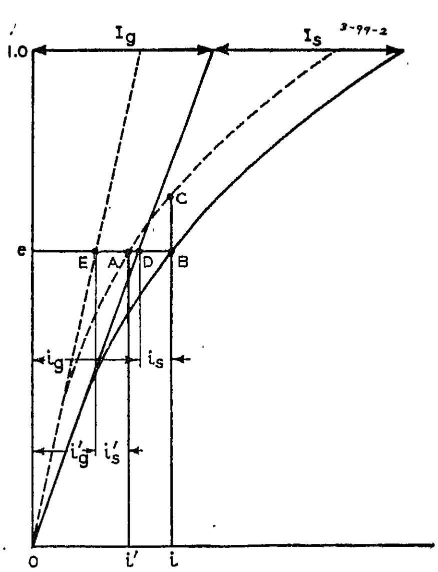


Figure 2. Air-gap flux-density change from the no-load saturation curve

B. Unbalanced Pull for Three or More Parallels in the Primary Winding

Assume a constant gap density under one parallel of the winding, and assume that the gap is changed a constant amount over this section. It is shown in Appendix II that the flux density rise is given by the formula:

$$b=\frac{\frac{\Delta}{g}eB_g}{1+me^{m-1}\frac{I_s}{I_g}+\frac{1}{I_gX_1}}$$

This may also be written in the form:

$$b=K_R\frac{\Delta}{g}eB_g$$

where

$$K_R=\frac{1}{1+me^{m-1}\frac{I_s}{I_g}+\frac{1}{I_gX_1}}$$

and represents the reduction in density rise because of saturation and primary reactance.

It is shown in Appendix III that if there are n parallels and r be any one of these parallels, the total pull of the rth parallel along a given line is given by the formula:

Total pull of rth parallel =

$$1.84\times10^{-4}e^2\frac{B_g{}^2A}{g}\left[\frac{\sin r\frac{4\pi}{n}-\sin(r-1)\frac{4\pi}{n}}{4}\times K_s+(K_R-K_S)\frac{n}{2\pi}\left\{\sin r\frac{2\pi}{n}-\sin(r-1)\frac{2\pi}{n}\right\}^2+\frac{\pi}{n}K_s\right]$$

For various numbers of parallels, this formula has the following forms:

3 parallels: unbalanced pull =

$$2.88\times10^{-4}e^2\frac{B_g{}^2A}{g}\;[0.315K_s+0.685K_R]$$

4 parallels: unbalanced pull =

$$2.88\times10^{-4}e^2\frac{B_g{}^2A}{g}\;[0.188K_s+0.812K_R]$$

6 parallels: unbalanced pull =

$$2.88\times10^{-4}e^2\frac{B_g{}^2A}{g}\;[0.087K_s+0.913K_R]$$

8 parallels: unbalanced pull =

$$2.88\times10^{-4}e^2\frac{B_g{}^2A}{g}\;[0.045K_s+0.955K_R]$$

12 parallels: unbalanced pull =

$$2.88\times10^{-4}e^2\frac{B_g{}^2A}{g}\;[0.022K_s+0.978K_R]$$

Infinite parallels: unbalanced pull =

$$2.88\times10^{-4}e^2\frac{B_g{}^2A}{g}\;[0K_s+1.0K_R]$$

Generally, the unbalanced pull may be expressed in the following form:

Unbalanced pull =

$$2.88e^2\frac{B_g{}^2A}{g}\;[aK_s+(1-a)K_R]\times10^{-4}$$

where a is determined from the curve of Figure 1.

From this equation, it is practically impossible to solve analytically for the voltage at which maximum pull will occur. However, a close approximation will be obtained, if we use in this formula the voltage at which the pull is a maximum, considering saturation alone.

With the constants that are used in most induction motors, the factor $[aK_s+(1-a)K_R]$ is approximately equal to K_s divided by the number of parallels. Therefore, the important fact is established that the unbalanced pull for an induction motor having three or more parallels in the primary winding may be obtained approximately by calculating the pull for a series winding and then dividing by the number of parallels.

II. Synchronous Motors

Synchronous motors have three distinct modes of operation as follows:

1. On open circuit with any speed from zero to synchronous and any field current from zero to full load field current.
2. Starting as an induction motor with any speed from zero to synchronous and any applied voltage from zero to 110 per cent of normal.
3. Normal synchronous operation with any applied voltage from zero to 110 per cent of normal, any field current from zero to full-load field current and any load.

A. Unbalanced Pull for a Series or Two-Parallel Stator Winding

A synchronous motor with a series winding on open circuit follows the same type of analysis as the induction motor

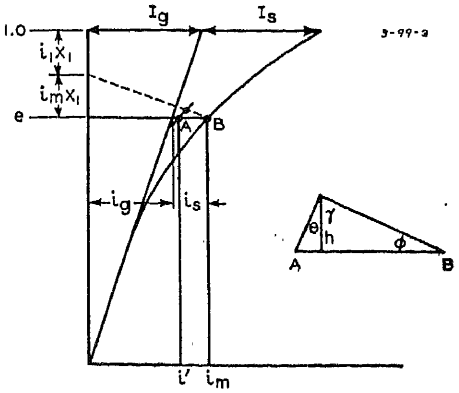


Figure 3. Flux-density change in one parallel of an induction motor

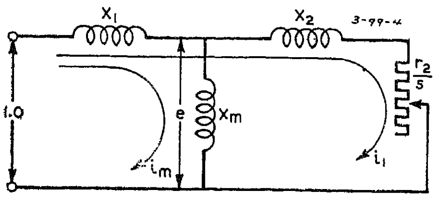


Figure 4. Equivalent circuit of the induction motor

with a series winding. Figure 2 and the equations derived from it apply except that they must be modified to take into account the field form of the synchronous motor. It has been found that this may be done quite accurately by multiplying the formulas for unbalanced pull previously given by C_1, the form factor of the wave.

Under conditions of starting or normal running, the relation between the air-gap flux density and the density in the iron parts of the machine may be different than the relation shown on the no-load saturation curve of Figure 2. However, as with induction motors, part of the iron carries higher flux than the air gap, while part carries lower. This tends to cancel out, and the magnetic pull calculated from the no-load saturation curve will be nearly correct for most machines.

Synchronous motors will normally be limited to about 120 per cent of normal air-gap flux density.

The following formulas, therefore, apply to a synchronous motor with a series or two-parallel stator winding:

1. For E less than 1.2

Maximum unbalanced pull =

$$2.88K_sE^2\frac{B_g{}^2A}{g}C_1\times10^{-4}$$

2. For E greater than 1.2

Maximum unbalanced pull =

$$3.11K_s\frac{B_g{}^2A}{g}C_1\times10^{-4}$$

B. Unbalanced Pull for Three or More Parallels in the Stator Winding

There is one very important difference between unbalanced pull in induction and in synchronous motors when parallels are present. That is that the parallels will not reduce the pull in a synchronous motor at standstill on open circuit and with the field excited. For a consideration of pulling-over of the rotor, this is then the worst condition of operation, and the machine must be calculated as though it had only a series winding in the stator.

However, for a consideration of critical speed of the shaft the running condition must be analyzed, and it is with this problem that the remainder of this section will deal.

A synchronous motor during the starting period will have an unbalanced magnetic pull which can be calculated in the same way as an induction motor. The following formulas will apply:

1. For E less than 1.1

Maximum unbalanced pull =

$$2.88E^2\frac{B_g{}^2A}{g}[aK_s+(1-a)K_R]\times10^{-4}$$

2. For E greater than 1.1

Maximum unbalanced pull =

$$2.62\frac{B_g{}^2A}{g}[aK_s+(1-a)K_R]\times10^{-4}$$

In the case of a synchronous motor it will be necessary to use these two formulas rather than merely dividing the unbalanced pull for a series winding by the number of parallels. This is due to the fact that the magnetizing current is normally much larger in synchronous than in induction motors.

Consider now the case of a synchronous motor running on open circuit with any field current and with, theoretically, an infinite number of parallels. It is apparent that with a change in air gap, the flux density rise will be $X_1/(X_1+X_m)$ times the density rise that would have occurred if no parallels had been present. The quantity X_1, $K_s/(X_1+X_m)$ therefore becomes the factor K_R for this condition of operation. The resistance of the stator winding has been neglected, but it should be noted that as zero speed is approached, the resistance limits the circulating current and brings the pull up to the full locked rotor value.

Suppose that the motor is now operated synchronously with a fixed voltage, field current, and load, and a small change is made in the air gap by displacing the rotor to one side. The reduction in air gap on one side releases a certain number of field ampere turns and a certain number of armature reaction ampere turns to force more flux through the machine. This tends to raise the flux in the air gap, but this increase in gap flux produces a current component in the stator parallel (not necessarily in phase with the main current) which produces a leakage reactance plus armature reaction voltage drop exactly equivalent to the original density rise. The phase angle of the current increment is such as to produce an armature reaction exactly in opposition to the original density rise. The net density rise will then be $X_1/(X_1+X_m)$ times the original density rise which gives the same K_R as for the open-circuit condition. As before, use of the no-load saturation curve for the determination of K_s is not exact but is a justifiable approximation.

Since the gap density will be limited to about 120 per cent of normal, we have two cases as follows:

1. For E less than 1.2

Maximum unbalanced pull =

$$2.88E^2\frac{B_g{}^2A}{g}[aK_s+(1-a)K_R]C_1\times10^{-4}$$

2. For E greater than 1.2

Maximum unbalanced pull =

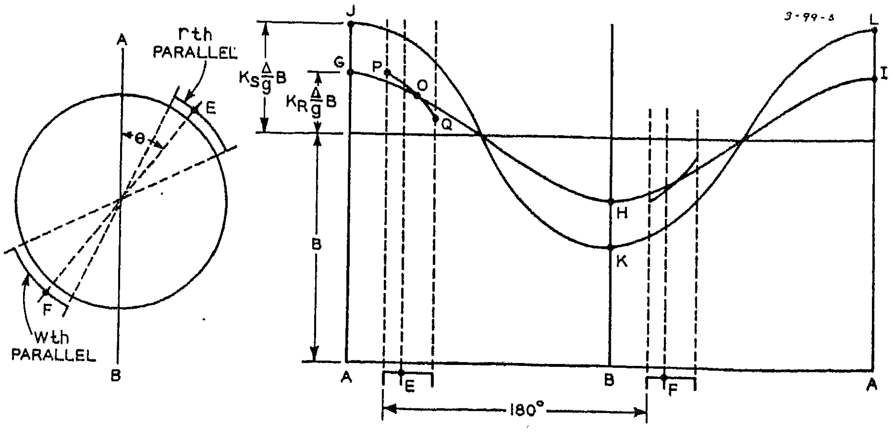
$$3.11\frac{B_g{}^2A}{g}[aK_s+(1-a)K_R]C_1\times10^{-4}$$

where

$$K_R=\frac{X_1}{X_1+X_m}K_s$$

For most machines, it is found that there is a larger magnetic pull during synchronous operation than during the starting period. Consequently, only this condition need be calculated in order to obtain the worst possible unbalanced pull.

Figure 5. Effect of parallels on the flux density change



Summary

Formulas have been given for calculating first, the per unit voltage at which maximum pull occurs, and then the maximum unbalanced magnetic pull for a synchronous or induction motor with any number of parallels in the stator winding. All quantities used in the formulas may be taken directly from the design sheet with the exception of the exponent *m* which may be calculated from any two known points on the no-load saturation curve.

In deriving the equations, it was assumed that the air-gap deflection was a static deflection resulting from such causes as misalignment of the rotor in the stator, or the stator being out of round. The same type of analysis may be applied to a deflection that rotates with the rotor, and the same equations for unbalanced pull will apply.

The effect of parallel circuits in the rotor has been neglected, but it should be noted generally that with either a static deflection or a rotating deflection at a speed less than synchronous, the pull will be further reduced below the calculated values.

A two-pole machine will have less unbalanced pull than calculated for certain conditions of operation, but the formulas given should still be used to calculate the worst possible condition.

A two-parallel stator winding has been taken as the same as a series winding because a deflection along the line between the parallels will produce no compensating currents, and the pull will not be reduced. However, it should be noted that the pull will be reduced below the calculated value if the parallels of the different phases are spaced symmetrically around the machine or if the deflection is at right angles to the line between the parallels.

It is apparent then that the formulas given are either correct or somewhat pessimistic for all conditions of operation that could be considered as normal. Special cases of unbalance from such causes as short-circuited coils cannot be treated in a general method and should be solved individually as they arise.

Appendix I

Referring to Figure 2, assume a constant gap density over a section of the machine, and consider that a constant change is made in the air gap while the magnetizing current is kept constant.

$i_g = CK_cg$ and $i_g' = CK_c'(g-\Delta)$

$$i_g' = \frac{K_c'}{K_c}\,\frac{g-\Delta}{g}\,i_g$$

$$AB = ED = i_g - i_g' = \left(1-\frac{K_c'}{K_c}\,\frac{g-\Delta}{g}\right) i_g$$

But $i_g = eI_g$ and $i_s = e^mI_s$

$$i' = i_g' + i_s' = \frac{K_c'}{K_c}\,\frac{g-\Delta}{g}\,eI_g + e^mI_s$$

$$\frac{di'}{de} = \cot\theta = \frac{K_c'}{K_c}\,\frac{g-\Delta}{g}\,I_g + me^{m-1}I_s$$

$$\frac{BC}{AB} = \tan\theta = \frac{1}{\dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g}\,I_g + me^{m-1}I_s}$$

$$BC = \frac{\left(1-\dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g}\right)eI_g}{\dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g}\,I_g + me^{m-1}I_s}$$

$$\frac{BC}{e} = \frac{b}{B} = \frac{\left(1-\dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g}\right)I_g}{\dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g}\,I_g + me^{m-1}I_s}$$

$$b = \frac{\left(1-\dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g}\right)eB_g}{\dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g} + me^{m-1}\dfrac{I_s}{I_g}}$$

Appendix II

Referring to Figure 3, assume a constant gap density under an independent section of the winding (a parallel), and assume that the gap is changed a constant amount over this section. The most pessimistic condition is obtained if in Figure 4 we assume that the secondary winding is open-circuited.

As proved previously:

$$i' = \frac{K_c'}{K_c}\,\frac{g-\Delta}{g}\,eI_g + e^mI_s$$

$$\frac{di'}{de} = \tan\theta = \frac{K_c'}{K_c}\,\frac{g-\Delta}{g}\,I_g + me^{m-1}I_s$$

$$\tan\phi = \frac{i_mX_1}{i_m} = X_1 \quad \tan\gamma = \frac{1}{X_1}$$

$$AB = \left(1-\frac{K_c'}{K_c}\,\frac{g-\Delta}{g}\right) i_g = h\tan\theta + h\tan\gamma$$

$$h = \frac{AB}{\tan\theta + \tan\gamma}$$

$$= \frac{\left(1-\dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g}\right)eI_g}{\dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g}\,I_g + me^{m-1}I_s + \dfrac{1}{X_1}}$$

$$\frac{h}{e} = \frac{b}{B} = \frac{1-\dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g}}{\dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g} + me^{m-1}\dfrac{I_s}{I_g} + \dfrac{1}{I_gX_1}}$$

If a small deflection is considered

$$b = \frac{\dfrac{\Delta}{g}\,eB_g}{1 + me^{m-1}\dfrac{I_s}{I_g} + \dfrac{1}{I_gX_1}}$$

Appendix III

Let there be *n* parallels total in each phase

Angular spread of each parallel $= 2\pi/n$ radians.

In Figure 5 the equation of curve *JKL* is $K_s(\Delta/g)B\cos\theta$. The limits on θ for the *r*th parallel are $(r-1)2\pi/n$ and $r2\pi/n$. The average value of *JKL* over the *r*th parallel =

$$\frac{1}{\dfrac{2\pi}{n}}\int_{(r-1)\frac{2\pi}{n}}^{r\frac{2\pi}{n}} K_s\frac{\Delta}{g}B\cos\theta d\theta$$

$$= \frac{n}{2\pi}K_s\frac{\Delta}{g}B\left[\sin r\frac{2\pi}{n} - \sin(r-1)\frac{2\pi}{n}\right]$$

Similarly the average value of *GHI* over the *r*th parallel

$$= \frac{n}{2\pi}K_R\frac{\Delta}{g}B\left[\sin r\frac{2\pi}{n} - \sin(r-1)\frac{2\pi}{n}\right]$$

The drop in density under the *r*th parallel from the curve *JKL* to the final position at *POQ*

$$= (K_s-K_R)\frac{n}{2\pi}\frac{\Delta}{g}B\left[\sin r\frac{2\pi}{n} - \sin(r-1)\frac{2\pi}{n}\right]$$

Equation of $POQ =$

$$K_s\frac{\Delta}{g}B\cos\theta - (K_s-K_R)\frac{n}{2\pi}\frac{\Delta}{g}\times B\left[\sin r\frac{2\pi}{n} - \sin(r-1)\frac{2\pi}{n}\right]$$

Assuming a large number of poles compared to the number of parallels and that part of the saturation is in the teeth of the machine, it may be readily proved that the pull along the line *AB*

$$= \frac{0.01386}{\pi}\left(\frac{4}{3}\right)BbA\cos\theta d\theta$$

For the *r*th parallel:

$$b = \frac{\Delta}{g}B\,[K_s\cos\theta - (K_s-K_R)\mathrm{B}]$$

where

$$\mathrm{B} = \frac{n}{2\pi}\left\{\sin r\frac{2\pi}{n} - \sin(r-1)\frac{2\pi}{n}\right\}$$

Total pull of the *r*th parallel along the line *AB*

$$= \frac{0.01386}{\pi}\left(\frac{4}{3}\right)B^2\frac{\Delta}{g}A\int_{(r-1)\frac{2\pi}{n}}^{r\frac{2\pi}{n}}\{K_s\cos^2\theta - (K_s-K_R)\beta\cos\theta\}d\theta = 1.84\times10^{-4}e^2\frac{B_g{}^2A}{g}\times$$

$$\left[\frac{\sin r\dfrac{4\pi}{n} - \sin(r-1)\dfrac{4\pi}{n}}{4}K_s + (K_R-K_s)\frac{n}{2\pi}\times\left\{\sin r\frac{2\pi}{n} - \sin(r-1)\frac{2\pi}{n}\right\}^2 + \frac{\pi}{n}K_s\right]$$

Characteristics and Applications of Selenium-Rectifier Cells

E. A. HARTY
MEMBER AIEE

Synopsis: The rectification properties of selenium cells were first discovered in the year 1883. C. T. Fritts described them in the *American Journal of Science*.[11,12] However, they were never used to any extent, except possibly as photocells.

After the introduction of the copper-oxide rectifier, research activities were stimulated, and selenium cells were again rediscovered. The first commercial cells were made in Germany in the early '30's, and, as the technique of manufacture improved, better cells were made with better life expectancy.

The General Electric Company, after a period of developmental activity, started to make cells in 1938, first in its research laboratories, and subsequently a manufacturing plant was set up which permits producing cells in large quantities within relatively close electrical tolerances.

This paper contains data pertaining to these cells particularly, and the information may not apply in detail to cells manufactured by other methods without some correction factors.

CONSTRUCTION

THE selenium rectifier cell consists essentially of a carrier plate made of either aluminum or iron, supporting on one side a very thin film of specially treated selenium. The adhesion is quite intimate to prevent contact losses. This selenium film is given a series of controlled heat treatments to obtain a suitable crystalline structure.

Finally, a low melting point alloy is metal-sprayed onto the selenium surface. This layer is known as the "counterelectrode."

By means of subsequent electrochemical processes a film or blocking layer is formed between the counterelectrode and the selenium surface. Current flows freely between the selenium and the counterelectrode and is practically blocked in the other direction. Figure 1 shows a typical cross section of a cell.

THEORY

Several theories have been suggested to explain the action of rectifiers of the type under discussion. The physicists do not all agree, and, therefore, no attempt will be made to discuss all these theories in detail.

One of the theories that appears the most logical to understand and that applies to all types of metal rectifiers is the following:

Metal rectifiers consist essentially of a semiconductor and a good conductor separated by a barrier or blocking layer which is, in itself, an insulator but through which electrons can pass in either direction. In the selenium rectifier the selenium layer is the semiconductor and the sprayed-metal counterelectrode the good conductor, the barrier or blocking layer being formed between these two substances. The sprayed-metal layer has an abundance of free electrons, while in the selenium layer, which is a relatively poor conductor, the free electrons are quite small.

When the two electrodes are connected to a source of supply, the opposite polarities set up an electric field across the barrier or blocking layer. Since this layer is very thin, a comparatively small electromotive force will produce a steep potential gradient. If the sprayed metal

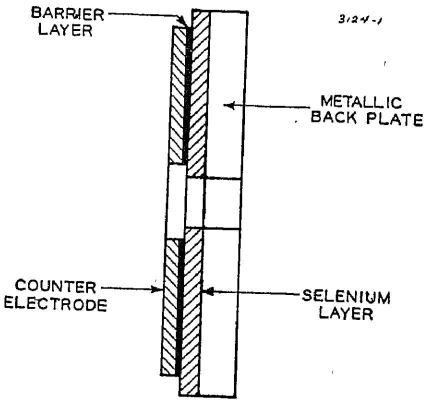


Figure 1. Cross section of a selenium cell

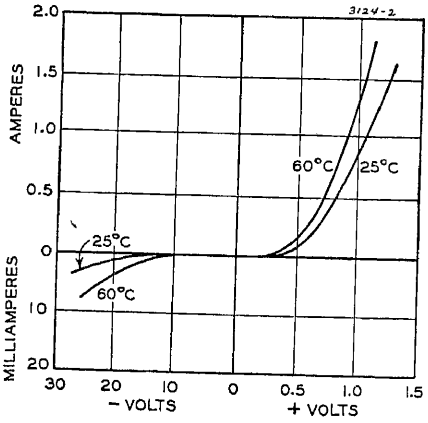


Figure 2. D-c characteristics

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Appendix IV. Nomenclature

A—Area of the stator bore in square inches.
g—Single air gap in inches.
g'—Single air gap with he rotor displaced $(g-\Delta)$ in inches.
Δ—Amount the rotor is displaced from the center of the stator in inches.
K_c—Carter's coefficient for an air gap g.
K_c'—Carter's coefficient for an air gap g'.
B_g—Air-gap flux density at normal voltage in kilolines per square inch.
B—Air-gap flux density at any voltage.
b—Rise in the gap flux density.
e—Voltage in per unit corresponding to air-gap flux.
E—Per unit voltage at which maximum pull occurs.
I_g—Magnetizing current for the air gap at normal voltage.
I_s—Magnetizing current for saturation at normal voltage.
i_m—Magnetizing current at any voltage.
X_1—Stator leakage reactance in per unit.
X_m—Magnetizing reactance in per unit.
C_1—Form factor of the no-load field form of a synchronous motor.
m—Saturation curve exponent defined by $i_s = e^m I_s$.
K_s—Factor allowing for the effect of saturation on density rise.
K_R—Factor allowing for the effect of saturation and primary reactance on density rise.
a—Factor depending on the number of parallels.

References

1. MAGNETIC PULL IN ELECTRIC MACHINES, E. Rosenberg. AIEE TRANSACTIONS, volume 37, 1918, pages 1425–69.

2. CRITICAL REVIEW OF THE BIBLIOGRAPHY ON UNBALANCED MAGNETIC PULL IN DYNAMO-ELECTRIC MACHINES, Alexander Gray, J. G. Pertsch, Jr. AIEE TRANSACTIONS, volume 37, 1918, pages 1417–24.

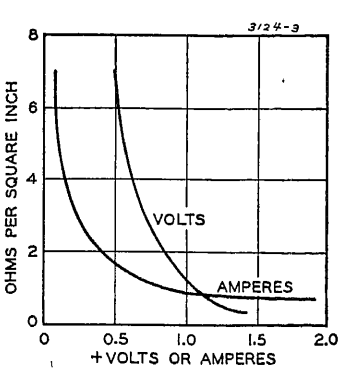


Figure 3. Resistance change with voltage or current

is connected to the negative pole of the source, the free negative electrons are accelerated to a sufficient velocity to enable them to pass through the barrier layer and the intercrystalline spaces of the selenium and reach the metal supporting disk, with the result that a flow of electrons is established which constitutes a current of electricity in the forward direction. When the polarity is reversed, the same action takes place in the opposite direction, but, since the number of free electrons in the semiconducting selenium is less than in the metal disk, the resulting current is much smaller. Because of this asymmetrical property, it is possible to rectify alternating current.

Electrical Characteristics of the Elements

Because it is almost impossible to make all cells exactly alike as to characteristics, even though materials and manufacturing methods are held within very close limits, the data that follow and also all published curves should be considered as representing average conditions. A slight deviation should be expected between individual cells.

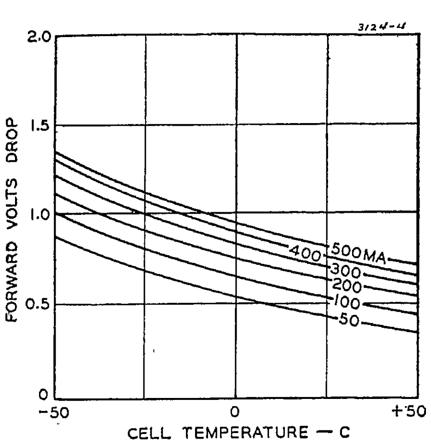


Figure 4. Temperature-forward voltage characteristics

One square inch area

D-C Characteristics

If a d-c voltage of varying potential is impressed first in the blocking direction and then in the flow direction and readings taken of the current that flows, a curve as shown in Figure 2 is obtained. This curve is based on selenium cells having one square inch of effective rectifying area.

In a similar way Figure 3 shows resistance versus voltage and resistance versus current. It should be noted and remembered that these curves are nonlinear and that they do not obey Ohm's law.

Effect of Temperature

The selenium rectifier cells have negative temperature coefficient which results in current increasing for a given voltage as the temperature goes up and decreasing as the temperature goes down.

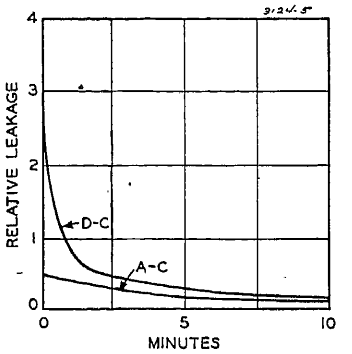


Figure 5. Leakage-time characteristics

Figure 4 shows this relation.

It should be noted that temperature also affects the leakage current in the blocking direction as shown on Figure 2.

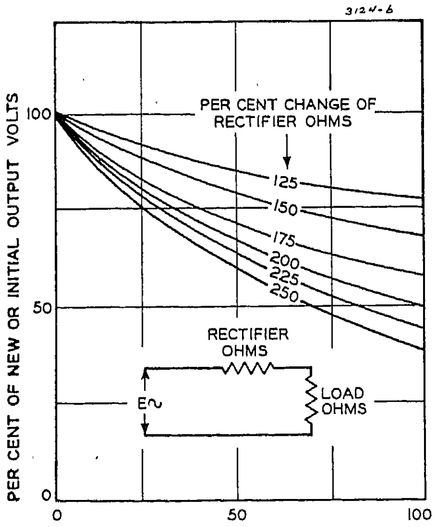


Figure 6. Effect of changing rectifier resistance on output

Because of this characteristic, ratings should be such that the losses caused by heating should be kept under control; otherwise they may keep on adding, resulting in overheating and the eventual destruction of the rectifying film. In general, it may be stated that the rectifier will operate satisfactorily in a range of ambient temperature from −50 to +50 degrees centigrade.

Forward Characteristics and Stability

The forward characteristic is very stable when a-c or d-c voltage is applied in this direction. However, as the cell heats up, changes occur. Over a period of time, the resistance of the cell appears to change and take a set. The rate of change increases with cell temperature, and at 100 degrees centigrade the cell is damaged.

An increase in forward resistance with time means that the difference between the input voltage and the output voltage will become greater. Therefore, to maintain a given output voltage constant, it is necessary to increase the input voltage. Great care must be taken to rate correctly these rectifier cells when new to prevent overloading in voltage after aging takes place.

Reverse Characteristics and Stability

The reverse or leakage characteristic on alternating current is quite good; it is, in general, higher initially but creeps to a minimum value in about two to three minutes. However, when a selenium rectifier cell is subjected to a d-c voltage in the inverse direction, the leakage current is very high initially. A polarizing action similar to that observed with electrolytic capacitors takes place, resulting in a steady decrease in current with time. Typical leakage voltage–time character-

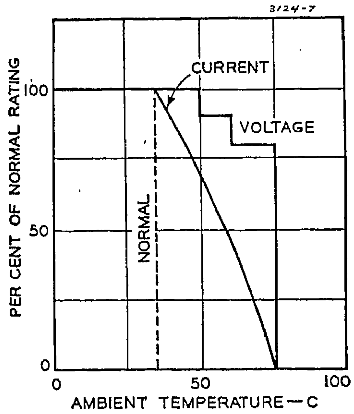


Figure 7. Rating of cells at high ambients

istics are shown on Figure 5. When the cells are continuously de-energized over a period of time, the reverse leakage resistance tends to decrease but will rapidly increase after the cells are again energized. It should again be noted that this same type of characteristic is also present in electrolytic capacitors. This characteristic should be borne in mind when applying these cells in blocking circuits, especially if instantaneous operation is required such as in certain types of high-speed relaying circuits.

CAPACITANCE

A certain capacitance exists because of the presence of the barrier or blocking layer between the two electrodes of the rectifier cell. Measurements indicate this to be of the order of 0.02 microfarad per square centimeter. This may vary somewhat, depending on the past history of the cell.

In high-frequency applications, the capacitance acts like a shunt acrosss each cell, resulting in lowering the leakage resistance and changing the ratio of the forward to the reverse resistance. At normal a-c frequencies up to 2,000 cycles, the capacity effect usually can be disregarded.

Rectifier Circuits

Selenium rectifier cells are readily combined into series and parallel groups, depending on the voltage and current output required. These are used in rectifier circuits to change alternating currents to direct currents. The choice of the particular rectifier circuit rests with the designer. In view of the fact that rectifiers are used by many engineers who are not familiar with these circuits, a tabulation of the more popular circuits is included with this paper. (See Figure 19.)

It should be noted that these data apply to perfect sine waves, perfect rectifiers having no internal resistance and also no leakage whatsoever in the blocking direction. The load is based on a noninductive resistance.

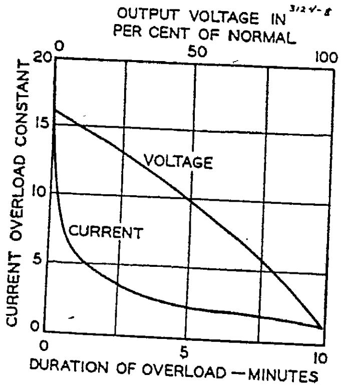


Figure 8. Overload rating of selenium cells

Calculations made with the aforementioned data are only of theoretical use and cannot be applied to actual design work without using correction factors.

Selenium rectifier cells have a fundamental resistance characteristic; this resistance is not constant and varies with current, temperature, and also time. Therefore, computations arrived at from formulas are not absolute, unless all these variables are considered. Design work should be based on empirical data based on years of experience to avoid trouble. Any correction factors must also have their own correction factors, since these will vary with time, temperature, and current.

In designing a practical selenium rectifier circuit, it is evidently very essential to pay particular attention to the fact that the rectifier resistance is not constant and changes with time, current, and temperature.

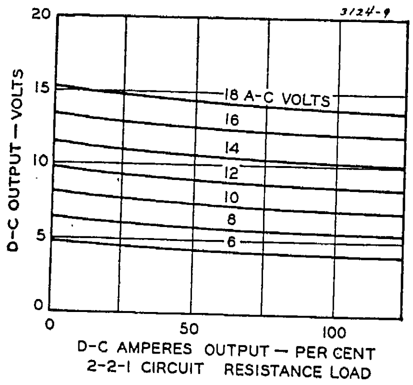


Figure 9. A-c to d-c characteristics of single-phase unit bridge rectifier with resistance load

One square inch area, 25 degrees centigrade

To minimize its effect on the rectifier circuit, it is quite essential to make the rectifier resistance small as compared to the total circuit resistance.[4] This is shown graphically in Figure 6.

The recommended ratings have been so chosen as to make the rectifier resistance about 10 to 15 per cent of the circuit resistance.

By referring to Figure 6, it can be noted that even though the rectifier resistance may double, the effect on the circuit is small.

However, when rectifiers are overloaded, the rectifier resistance in effect becomes a larger percentage of the circuit resistance, and, therefore, more changes should be expected in the output as readily shown in Figure 6.

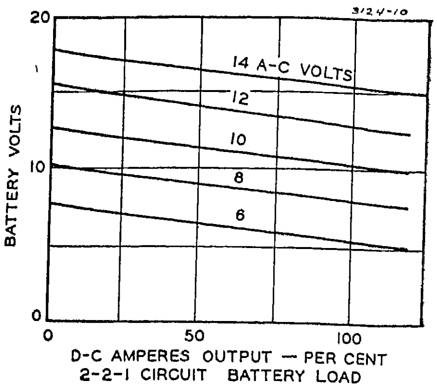


Figure 10. A-c to d-c characteristics of single-phase unit bridge rectifier with battery load

One square inch area, 25 degrees centigrade

MAXIMUM RATINGS

Table I and II show the voltage and current ratings for the various size cells which are available at the present time.

The circuit names as used by the metallic rectifier industry have been retained in Tables I and II.

The circuit symbolic notation is an attempt to overcome the objections raised to the circuit names by replacing them by a symbolic equation 3.

The first digit is the reciprocal of the fraction of time a cell carries current during the cycle. The second digit shows the cells in series and the third digit the cells in multiple carrying current instantaneously. This notation applies to unit rectifiers.

These ratings are based on the usual limiting factors of temperature rise, aging, and voltage breakdown. The in-

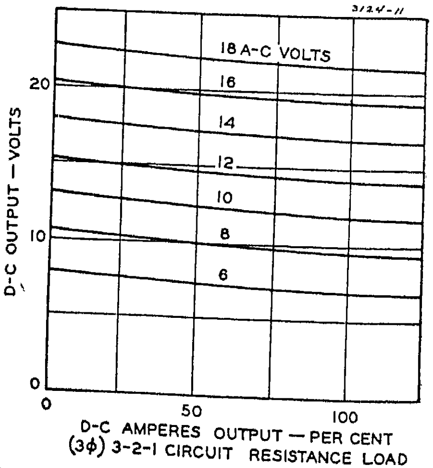


Figure 11. A-c to d-c characteristics of three-phase rectifier, bridge type, with resistance load

One square inch area, 25 degrees centigrade

Table I. Rating as Valves at 35 Degrees Centigrade

Code letter	*F*	*A*	*C*	*H*
Diameter of cells	1 Inch	$1^1/_2$	$2^3/_{16}$	$4^3/_8$
Inverse rms volts	18	18	18	18
Blocking volts d-c	15	15	15	15

ternal losses heat the rectifier cell, and the final permissible total temperature limits the output rating. It should be borne in mind that aging increases the heating, and, therefore, it is very important to run the rectifier cells somewhat cooler when new. This factor has been taken into consideration in the published ratings. The relation of cell spacing to cell diameter in a rectifier-stack assembly has been chosen for optimum cooling.

PERMISSIBLE TEMPERATURE RISE

Although selenium rectifier cells may be operated up to a total temperature of 75 degrees centigrade, the ratings given in the previous paragraph are based on an ambient temperature of 35 degrees centigrade allowing a maximum temperature rise of 40 degrees centigrade. However, if the normal temperature is liable to be exceeded, the full-load ratings must be changed and reduced to prevent the total temperature from exceeding 75 degrees centigrade.

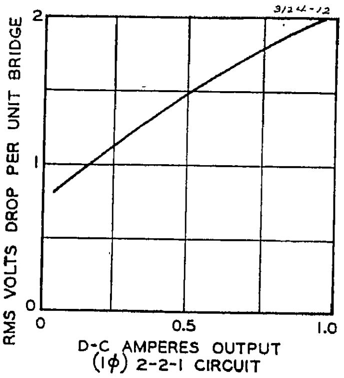


Figure 12. Rms volts drop in a unit bridge single-phase rectifier at different values of current

One square inch area, 25 degrees centigrade

Figure 7 shows the recommended practice.

OVERLOAD AND INTERMITTENT RATING

Selenium rectifier cells will withstand short-time current overloads beyond the normal current, provided the cell is not heated above 75 degrees centigrade. If the cell is allowed to cool back to normal between loading periods, higher current overloads can be applied than in the case of insufficient cooling periods.

Figure 8 shows permissible current overload data and also how the voltage drops as the current increases.

Voltage overloads are not permissible, even for short periods, because of the danger of breaking down the blocking layer.

If the breakdown current is limited, the punctured cells sometimes self-heal. However, every healed spot robs the rectifying surface of some cross section, resulting in increasing effective resistance of the cell.

UNIT RECTIFIER

For convenience in designing rectifiers, data have been prepared on a unit rectifier. This can be defined as any rectifier circuit having one cell in each arm. By reducing all measurements to a unit rectifier, it is possible to obtain general data that will apply to any rectifier, providing the number of cells in series in each arm is known. These same data can also be applied where two or more unit rectifiers are operated in multiple.

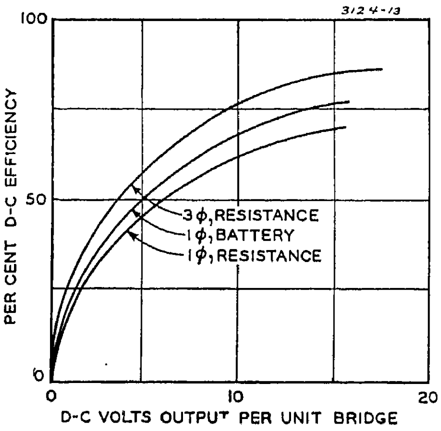


Figure 13. Efficiency of selenium cells at full-load current

Full-load amperes, 25 degrees centigrade

A-C–D-C CHARACTERISTICS OF A SINGLE-PHASE FULL-WAVE UNIT BRIDGE RECTIFIER WITH A RESISTANCE LOAD

By impressing various voltages across a unit bridge rectifier, a group of curves may be obtained as in Figure 9, showing d-c voltages plotted against d-c amperes output for various impressed a-c voltages.

A-C–D-C CHARACTERISTICS OF A SINGLE-PHASE FULL-WAVE UNIT BRIDGE RECTIFIER AS A BATTERY CHARGER

If the resistance load is replaced by a battery load, and data as shown previously are again repeated, a family of curves as shown in Figure 10 is obtained.

A-C–D-C CHARACTERISTICS OF A THREE-PHASE FULL-WAVE UNIT BRIDGE RECTIFIER

If a three-phase rectifier is operated at different a-c voltages and different loads, a family of curves as shown in Figure 11 is obtained.

A-C VOLTS DROP WITHIN A UNIT BRIDGE RECTIFIER

If a d-c ammeter is used as a load across a unit bridge rectifier and the a-c input is varied, a curve as shown in Figure 12 is obtained. This permits calculating the a-c input voltage provided correction factors are used, depending on the circuit and its form factor.

EFFICIENCY

Because of the presence of an a-c component in the d-c output of all rectifiers, it is quite essential to use the correct type

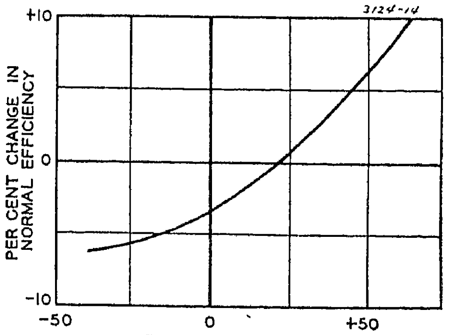


Figure 14. Correction curve to obtain efficiency at different temperatures

Table II. Current Ratings at 35 Degrees Centigrade for Resistance and Inductance Loads

Circuit	1 Inch	$1^1/_2$ Inches	$2^3/_{16}$ Inches	$4^3/_8$ Inches	D-C Volts	Circuit Symbolic Notation
One-phase half-wave	0.075	0.2	0.500	2.15	6	1-1-1
One-phase bridge	0.150	0.4	1.00	4.3	12	2-2-1
One-phase center tap	0.150	0.4	1.00	4.3	6	2-1-1
Three-phase half-wave	0.200	0.5	1.25	5.3	8	3-1-1
Three-phase bridge	0.220	0.600	1.4	6.5	16	3-2-1
Three-phase center tap with no interphase coil	0.270	0.7	1.8	8.0	8	6-1-1
Three-phase center tap and interphase coil	0.400	1.0	2.5	11.0	8	3-1-2
D-c valves	0.120	0.320	0.80	3.0	15	1-1-1

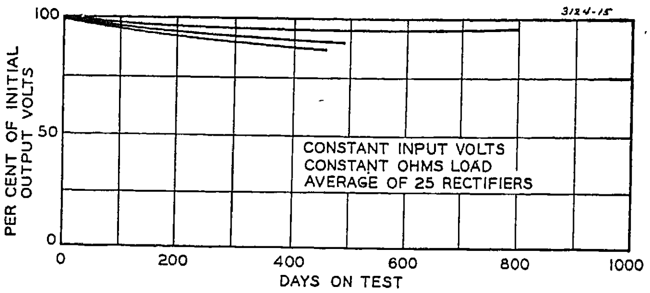


Figure 15. Aging of selenium rectifiers

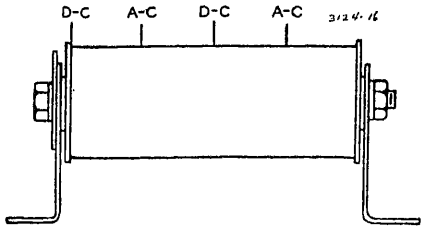


Figure 16 (right). Full-wave single-phase rectifier-stack assembly

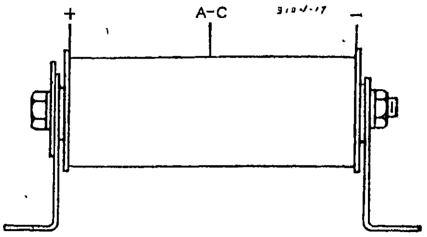


Figure 17 (right). Half-wave three-terminal-type rectifier-stack assembly

of instruments to measure the power output.

The input is always measured with an a-c wattmeter and reads all of the power delivered to the rectifier.

If the nature of the load is such that both the a-c and d-c components of the rectifier output produce useful work, then an a-c wattmeter in the output measures the correct output. Examples of this type of load are resistances, lamps, radio-tube filaments, electromagnets, series motors, and so forth.

However, if the a-c component is not utilized, then the output should be measured with a d-c voltmeter and d-c ammeter of the D'Arsonval type. For example, battery charging, plating work,

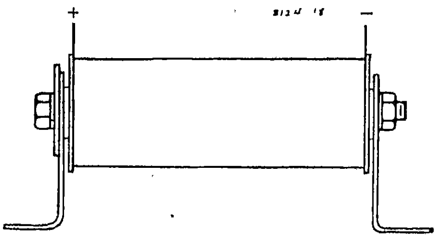
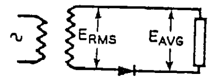
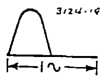
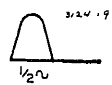
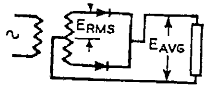
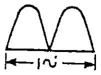
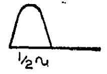
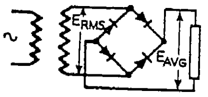
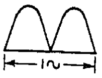
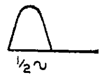
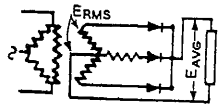
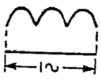
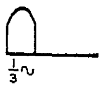
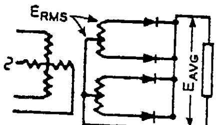
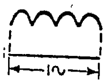
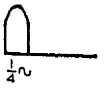
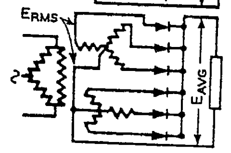
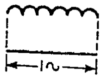
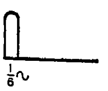
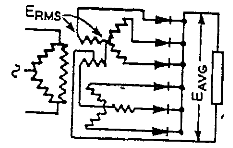
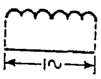
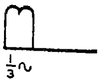
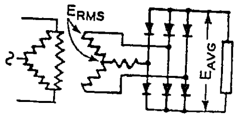
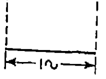
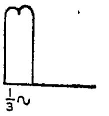


Figure 18 (right). Half-wave two-terminal-type rectifier-stack assembly

Figure 19 (below). Fundamental rectifier circuits

Symbolic Notation	Circuit	Output Wave Values: Wave Form	E_{avg}	E_{eff}	Form Fact.	D-C Ripple %	Rectifier Cell-Wave Values: Wave Form	I_{avg}	I_{eff}	Form Fact.	E Inverse	Trans. Capacity: Pri.	Sec.
1-1-1			0.45 E_{rms}	0.707 E_{rms}	1.57	121		1.00 I_{dc}	1.57 I_{dc}	1.57	1.414 E_{rms}	3.49 EI_{dc}	3.49 EI_{dc}
2-1-1			0.900 E_{rms}	1.0 E_{rms}	1.11	48		0.500 I_{dc}	0.786 I_{dc}	1.57	2.828 E_{rms}	1.235 EI_{dc}	1.75 EI_{dc}
2-2-1			0.900 E_{rms}	1.0 E_{rms}	1.11	48		0.500 I_{dc}	0.786 I_{dc}	1.57	1.414 E_{rms}	1.235 EI_{dc}	1.235 EI_{dc}
3-1-1			1.17 E_{rms}	1.19 E_{rms}	1.02	21		0.333 I_{dc}	0.587 I_{dc}	1.76	2.45 E_{rms}	1.508 EI_{dc}	1.508 EI_{dc}
4-1-1			1.27 E_{rms}	1.28 E_{rms}	1.005	11		0.250 I_{dc}	0.502 I_{dc}	2.01	2.828 E_{rms}	1.116 EI_{dc}	1.58 EI_{dc}
6-1-1			1.350 E_{rms}	1.351 E_{rms}	1.001	4		0.167 I_{dc}	0.408 I_{dc}	2.45	2.828 E_{rms}	1.28 EI_{dc}	1.81 EI_{dc}
3-1-2			1.170 E_{rms}	1.170 E_{rms}	1.001	4		0.167 I_{dc}	0.293 I_{dc}	1.76	2.45 E_{rms}	1.068 EI_{dc}	1.51 EI_{dc}
3-2-1			2.340 E_{rms}	2.341 E_{rms}	1.001	4		0.333 I_{dc}	0.579 I_{dc}	1.74	2.45 E_{rms}	1.047 EI_{dc}	1.047 EI_{dc}

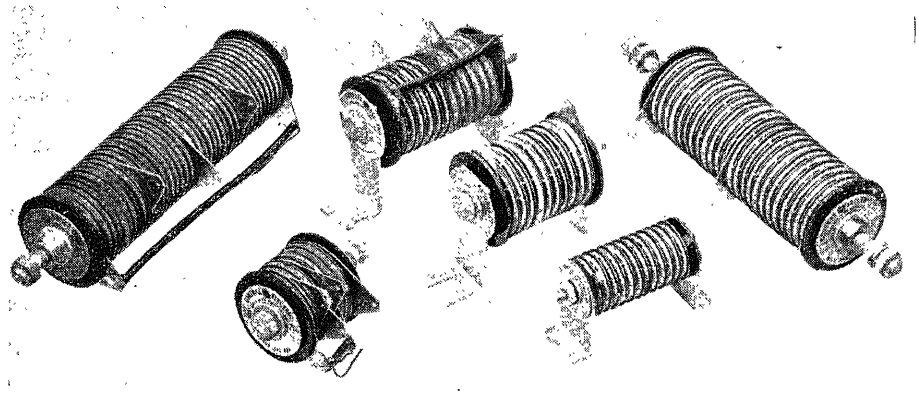


Figure 20. Typical selenium rectifiers for industrial applications

chemical work, electrolysis, shunt motors, and so forth. Therefore, the efficiency can be expressed as either

$$\text{RMS efficiency } \% = \frac{\text{a-c watts output}}{\text{a-c watts input}} \times 100$$

or

$$\text{D-c or average efficiency } \% = \frac{\text{d-c volts} \times \text{d-c amperes}}{\text{a-c watts input}} \times 100$$

In polyphase work, the rms efficiency approximately equals the average efficiency.

However, in single-phase work, there is a difference of approximately 15 points in efficiency between the average and rms values, the latter being the highest.

Typical efficiency curves are shown in Figure 13 at different voltages and at full-load current densities. Figure 14 shows correction factors at different temperatures.

Life Tests

General Electric selenium rectifiers have undergone extensive tests and show promise of better aging characteristics than some of the European elements previously tested.[4] Typical life tests are shown in Figure 15. It should be noted that these tests represent aging as seen from the user's viewpoint.

Practical Rectifier Design

It is believed that a worked-out example will show how to use the data contained in this paper.

For example, assume that we want a 30-volt rectifier at one ampere working on a resistance load from a single-phase circuit. By reference to the tabulation on ratings, we find that the most voltage we can obtain from a unit bridge rectifier at full current density for each type cell is 12 volts. Dividing 30/12 = 2.5, therefore, three is the minimum number of series cells to be selected.

The voltage to be supplied by each unit bridge rectifier will be 30/3 = 10. By reference to Figure 9, we find that, at full-load current and ten volts, we need 13.2 volts alternating current per bridge, or 3 × 13.2 = 39.6 for the rectifier under discussion.

The size of the cell can be determined by reference to the tabulation on ratings. To carry one ampere a type *C* cell having $2^3/_{16}$-inch diameter is required. If the current rating required does not match that of the four available sizes, then it will be necessary either to operate at slightly less than full rating or to use several smaller cells in multiple.

From the aforementioned calculations, to obtain 30 d-c volts at 1.0 ampere direct current, a bridge rectifier using three cells in series would be required. The a-c input is 39.6 volts, and the transformer should have sufficient taps to take care of line voltage variations. To take care of aging, it is advisable to tap the transformer up to 3×18 rms volts or 54 volts. The approximate efficiency is 63 per cent at 25 degrees centigrade as seen from Figure 13 and will be higher at the operating temperature as shown on Figure 14.

Construction of Selenium-Rectifier Stacks

Selenium rectifier cells are assembled into either full-wave or half-wave stacks, depending on their voltage and current rating. Low-voltage stack can generally be assembled into single stack, using a construction as shown in Figure 16.

For higher voltages, two stacks are sometimes used, embodying a construction as shown in Figure 17.

For still higher voltages four units are used as shown in Figure 18.

The cells are spaced to permit free ventilation on both sides, light spring washers being used to collect current.

The stacks are clamped under light pressure and come either with or without mounting brackets as shown in Figure 20.

Installation of Rectifier Stacks

Selenium rectifier stacks should be installed in well ventilated cabinets to permit free circulation of air.

They should be located preferably at the bottom of cabinets so that any heat from other heat dissipating apparatus does not have a cumulative effect.

If installed in closed cabinets, a certain amount of derating should be made to take care of the higher internal ambient temperatures as previously explained.

References

1. A New Form of Selenium Cells, C. T. Fritts. *American Journal of Science*, volume 26, 1883.

2. Sur les Elements et Piles au Selenium de Fritts, *Lumière Electrique*, volume 15, 1883.

3. Rectifier Terminology and Circuit Analysis, C. H. Willis, C. C. Herskind. AIEE Transactions, volume 61, 1942, July section, pages 496–9

4. Discussion by E. A. Harty of Selenium Rectifiers and Their Design. AIEE Transactions, volume 61, 1942, page 976.

5. Trockengleichrichter (book), Karl Maier. Oldenbourg, Munich, Germany, 1938.

A New High-Interrupting-Capacity Fuse for Voltages Through 138 Kv

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ASSOCIATE AIEE

Synopsis: The reliable performance of high-interrupting-capacity fuses in the intermediate voltage field has led to an increased interest in the application of such fuses for the higher voltages. Unfortunately, a fuse for the higher voltages cannot be made by simply enlarging or lenghthening the lower-voltage devices. To interrupt the higher currents in a reasonable structure, arc lengths must be kept short to reduce mechanical stresses. Interruption in these short distances creates high dielectric stresses external to the fuse that would endanger the operation of a device built along conventional lines. Adequate conductors for carrying the higher currents through the longer-length fuse must be moved quickly during interruption.

A new high-voltage fuse in which interruption of the higher currents is accomplished in a short length without undue voltage stresses adjacent to the fuse has been developed. This fuse has a new arrangement of conductors which permits rapid extension of the arc without expulsion of any conductor parts from the fuse. All this has been accomplished with a fuse of such light weight that it permits the use of a drop-out design which is a necessity at the higher voltages to prevent subsequent flashovers caused by leakage currents. The interrupting medium is compressed boric acid which has proved very effective and reliable at the lower voltages. Repeated interrupting and mechanical tests have demonstrated the suitability of the new fuse for service on important transmission circuits.

PRESENT day requirements relating to the transmission of large blocks of electrical energy to large industrial areas and new war industry plants remote from power generation plants demand short-circuit protective devices that will afford the maximum protection with a minimum outlay of critical materials. The excellent operating record established by modern high-interrupting-capacity power fuses in the intermediate voltage field has created an insistent demand for fuses suitable for application on transmission systems in the 44-kv to 132-kv class. Power fuses require less critical material than a conventional hook-stick-operated disconnect switch and can be mounted in the same space, thus effecting economies in substation space and structure. Certain operating characteristics of high-capacity power fuses provide for the maximum continuity of service so essential to modern production methods. Power fuses when applied for the short-circuit protection of transformers or circuits provide ultrahigh-speed clearing of faults. Faulted equipment is isolated from the system in a small fraction of the time required by conventional short-circuit protective devices, thus greatly reducing the damage to the equipment and enabling it to be repaired and returned to service at a much earlier date. Operating speeds equal to or less than the most modern ultra-high-speed circuit interrupters insure maximum system stability since the voltage dip associated with a fault persists for as short a period as one cycle. The short duration of voltage fluctuation insures that relays elsewhere on the system will not operate to stop important continuous process loads. The high-speed operation of power fuses reduces the requirements for elaborate relaying schemes on new transmission circuits and eliminates the need for extensive relay changes when new load centers are added to an existing transmission line.

Requirements for Higher-Voltage Power Fuses

Since electrical power circuits operating at 44 to 132 kv are intended for the transmission of large blocks of energy, it follows that such systems are connected to a generating source of considerable capacity, capable of delivering a correspondingly high value of short-circuit energy to a fault. Unlike conventional circuit breakers which require as long as eight cycles to interrupt a fault and are so rated, power fuses frequently operate in the first half cycle of a fault; hence they may have to interrupt a fully displaced or asymmetrical current. For the systems under discussion the rms value of this current may be as much as 1.6 times the symmetrical short-circuit current. This ratio of currents is dependent on the characteristics of the transmission circuit. The closer the fault is to the generating source the greater ratio of reactance to resistance and the higher the ratio of asymmetrical to symmetrical current which approaches the theoretical limit of 1.73. From this it can be seen that power fuses for use on high-voltage circuits must have an interrupting capacity considerably in excess of the symmetrical short-circuit current. To handle the most severe case a fuse for 1,000,000-kva systems must be able to interrupt at least 1.6 times the current value corresponding to a 1,000,000-kva symmetrical fault. It is obvious therefore that a fuse for use at the higher voltages

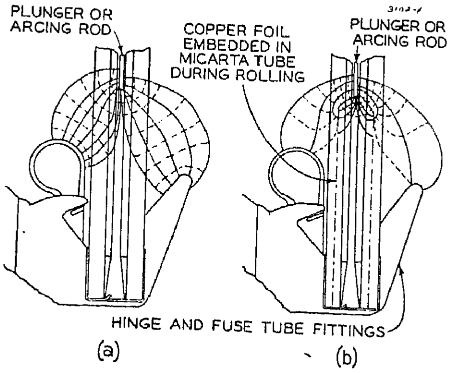
PLUNGER OR ARCING ROD
COPPER FOIL EMBEDDED IN MICARTA TUBE DURING ROLLING
PLUNGER OR ARCING ROD
HINGE AND FUSE TUBE FITTINGS
(a)
(b)

Figure 1. Approximate plot of dielectric field at lower end of fuse hit

Note how electrostatic shielding in diagram b reduces the lines of force or potential gradient at the lower fittings. The reduction of potential gradients at these points eliminates the possibility of external flashovers

(a). Without electrostatic shield

(b). With electrostatic shield

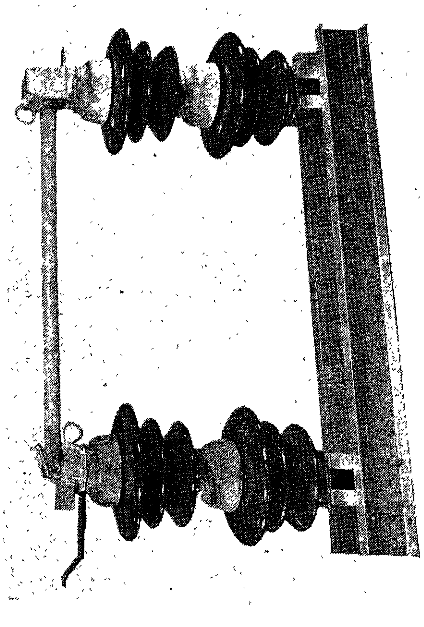

Figure 2. Single-pole unit, 92 kv, 200 amperes

Paper 43-103, recommended by the AIEE committee on protective devices for presentation at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943. Manuscript submitted April 7, 1943; made available for printing May 20, 1943.

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must have a high interrupting capacity.

A second requirement is the ability to interrupt positively the entire range of fault currents from the minimum melting current of the fusible element up to its full interrupting rating. While such fuses are normally applied solely for short-circuit protection, it is recognized that certain types of high impedance faults may develop, transformer secondary protective devices may be inoperative, or that faults may develop in a period of light generating capacity, requiring interrupting at low values of fault current.

A further requirement, especially important at these higher circuit voltages, is that the restored voltage shall not overstress the insulating material, making up the fuse tube so that there are high leakage currents which may result in flashover or dielectric breakdown. Since circuits of this voltage are sometimes supplied from hydroelectric plants where the sudden dropping of a large load may cause "runaway" of the generators before protective relays operate, the restored voltage may rise to a value double the normal circuit voltage. These conditions make it imperative that the higher-voltage fuses be automatically removed from the circuit immediately after operating.

Additional requirements particularly important to enable operating personnel to locate blown fuses and replace these with new units are positive indication of blown fuses and a lightweight simple unit that can readily be removed and replaced in the fuse support.

Evolution of the New Fuse

An analysis of the characteristics of the dry-type high-interrupting-capacity fuses for operating voltages up to 34.5 kv reveals that there are four major design features which have been responsible for its exceptional performance record.

1. Boric acid in the dry molded form as the lining of the interrupting chamber will interrupt a much higher voltage per inch of length than horn fiber on other fuse-tube materials in an equivalent structure. Since this is especially true in the lower range of currents, fuses utilizing boric acid can be made in a shorter length. At the higher currents this shorter-length boric-acid-lined fuse tube does not produce as vigorous a gas blast as the equivalent longer length of fiber; consequently, mechanical stresses in the tube structure are much less. In addition, the voltage gradient that can be interrupted increases with current much more rapidly. The gases evolved from boric acid are noncombustible and have a much higher dielectric strength than gases from organic materials and are much less liable to cause breakdown between adjacent line parts.

2. The very high interrupting capacity of this type of interrupter is largely due to the use of a very short fuse element in an enlarged opening of the fuse tube that is freely vented and the drawing of the arc into a more restricted section of the interrupting chamber. There is sufficient gas blast from the boric acid even in this enlarged opening to interrupt the higher currents at the end of the first half cycle of arcing without developing excessive pressures.

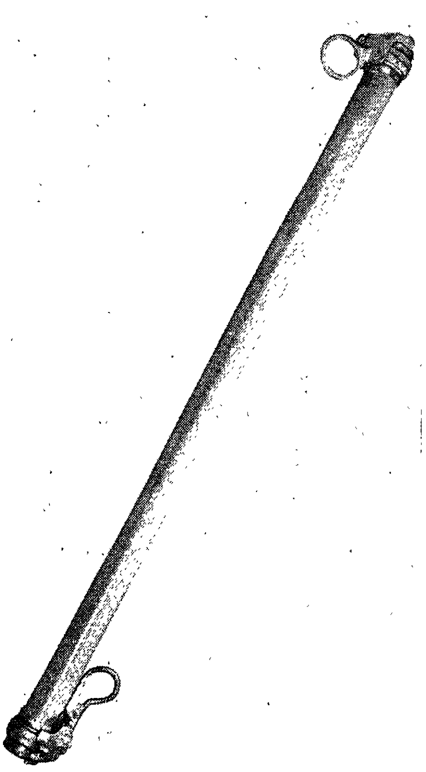


Figure 4. Fuse unit with disconnecting fittings

3. The use of a solid-rod-type conductor through the arcing chamber further increases reliability of high current interruptions, since there is no flexible conductor or cable to become jammed in the fuse tube by the gas pressure developed on very high fault currents.

4. Interruption of extremely high currents without excessive internal pressure and without severe voltage surges is further accomplished by the use of controlled mass and acceleration of the arcing terminal through the interrupting chamber. The mass of the arcing rod and of the operating spring and shunt assembly is so co-ordinated with the tension of the spring that the arcing rod is accelerated only a few inches or only a fraction of the length of the interrupter in the first half cycle. This short-length arc in the enlarged opening results in minimum mechanical stress in the fuse structure.

The first problem of the designer in converting low-voltage fuse structures to a design suitable for the higher voltage is to determine a suitable length of fuse. Since in the interests of simplicity of handling and replacement by operating personnel, it is desirable to have a disconnecting-type fuse, the minimum length of the fuse is automatically set by the standards for such types of equipment. The actual length of the fuse unit is then dependent upon the required length of interrupting chamber to secure efficient interruption throughout the entire current range. The voltage gradient that can be interrupted in a boric-acid interrupter varies with the diameter of the openings in the boric acid. Furthermore the voltage gradient is not constant for a given bore but varies with the length. For longer lengths, the voltage that can be interrupted corresponds to approximately the 1.7 power of the length. A large bore is required for in-

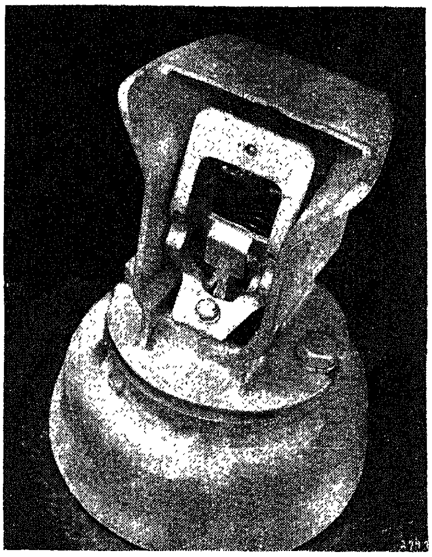


Figure 5. Sleet hood with dropout mechanism

Note the trigger which is actuated by the moving plunger to initiate the dropout action

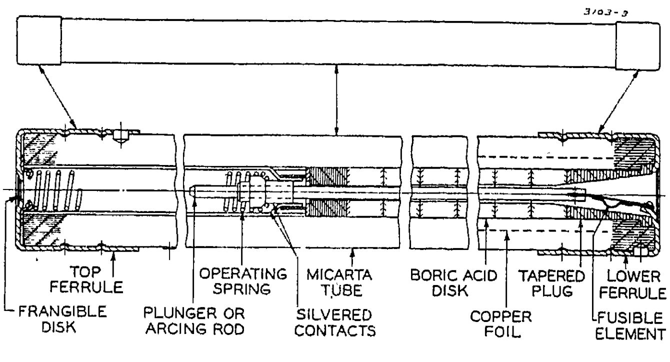


Figure 3. Cross section of new fuse unit

The unit for 138 kv is 72 inches long and 2 1/2 inches in diameter terruption of the higher fault currents to minimize mechanical stresses; however, it would be impractical to build such high-voltage fuses with a constant bore as the length required to interrupt currents in the lower range would be excessive. To make the fuse as short as possible, a stepped or tapered bore is used with the largest opening in the vented end so that gases are readily vented on high-current shots and the bore gradually decreasing to a diameter just larger than the size of the conductor rod. In this way, the fuse can be made just as short as the permissible open-gap spacing for a disconnecting-type unit, since the lower currents are readily interrupted when the arc is drawn into the more restricted bore. The use of the conventional spring and shunt assembly is not practical in the higher-voltage fuse as the collapsed length of such an assembly is approximately one third the length of the entire fuse unit. Therefore, to make the fuse unit as short as desired, a new method of conducting current from the top ferrule to the solid arcing rod has been developed which occupies a length equivalent to the length of the interrupting chamber.

After the minimum length has been established, the next step in the evolution of the higher-voltage fuses is to determine the form of the fuse unit. From a weight and handling standpoint, the use of insulating tube of organic material with a porcelain weatherproof casing is at once ruled out. Since it is recognized that organic insulation, such as a fuse tube, should not be subjected to high-voltage stress, such as restored voltage, after the fuse has blown, a drop-out-type fuse is indicated.

The decision to make a drop-out-type fuse entails further design problems, notably the necessity for a lightweight construction to eliminate any shock to the insulator column supporting the pivot end of the long fuse tube. The use of a permanent fuse holder with a renewable element or refill as has been used with such success in the lower-voltage fuses must be eliminated to avoid duplication of structure. In a design incorporating a renewable refill, the refill unit must be mechanically strong enough to withstand the entire pressure developed in the interupting chamber during interruption of the maximum fault current and as the pressures in the refill are communicable to the fuse holder, the holder must be of equally sturdy construction. The decision to make the higher-voltage fuses in a single nonrenewable unit was aided by early tests on a refill- and holder-type unit. With the lengths involved for the higher voltages it is virtually impossible to maintain the extremely close clearances between the outside of the refill tube and the inside of the holder tube required to prevent flashover in this space. The presence of ionized gases within the holder and the high-voltage gradients present in this space on high current interruption were very conducive to flashovers in this space. As the length of the interrupter increases, the voltage that can be interrupted increases faster than the dielectric strength of the adjacent air gap.

With lightweight construction a paramount requisite, it is imperative that the interrupter be very efficient so that the mechanical stresses resulting from gas pressures developed do not require an excessively strong and heavy structure. The efficiency of an interrupter is based on building up high dielectric strength in the arc path at current zero with low arc voltage and correspondingly low arc energy. An efficient fuse design is based on drawing an arc as short as possible in a structure offering a minimum of actual restriction to the arc core and develop sufficient un-ionized gases to effect interruption at first current zero. The interruption of high currents in such a short length imposes very severe dielectric stresses between the end of the arcing rod and the lower terminal of the fuse with its associated fittings.

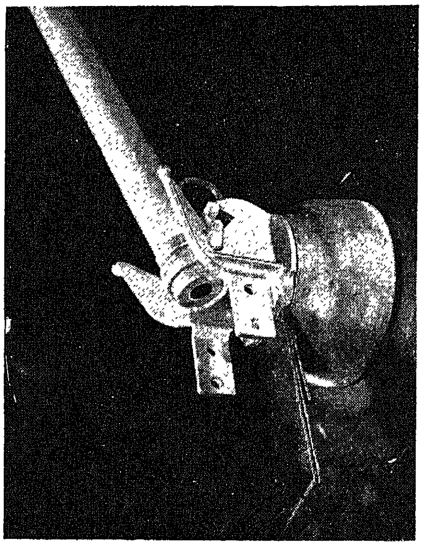


Figure 6. Hinge with fuse unit

To prevent external flashovers resulting from the presence of ionized air associated with such extreme voltage gradients, an electrostatic shield has been built into the tube structure. Figure 1*a* shows the voltage stress lines between the end of the arcing rod and the lower terminal. It is readily seen that very high voltage gradients are present at the fittings adjacent to the tube. The corona associated with such high-voltage stresses creates sufficient ionized air surrounding the tube to cause external flashover over the entire length of the fuse unit. Figure 1*b* shows the voltage-stress distribution with an electrostatic shield in the insulating tube. It will be observed that the maximum voltage gradient is now impressed on solid dielectric and not on the air adjacent to the fuse tube, so that external corona is eliminated.

A further requisite of the drop-out type of fuse construction, particularly for the voltages under consideration, is that the drop-out action must not start until the circuit is positively interrupted within the fuse unit. If the fuse unit should start to drop out before the current is interrupted, there will be arcing at the top contacts which would create sufficient ionized gases to cause flashover.

To secure positive time delay between the time of fusion of the calibrated element and the initiation of the drop-out action, a construction has been adopted whereby the drop-out action is not initiated until the moving arcing rod has reached the end of its travel.

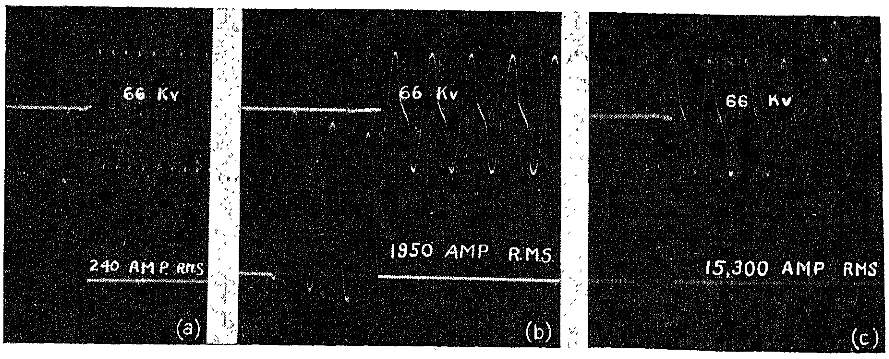


Figure 7. Oscillograms of interrupting tests on 69-kv fuse

(*a*). 240 amperes 66-kv restored voltage

(*b*). 1,950 amperes 66-kv restored voltage

(*c*). 15,300 amperes 66-kv restored voltage

Construction and Operation

A complete fuse consists of a fuse unit which is nonrenewable and is replaced by an entirely new unit after fault interruption and a fuse mounting which includes the necessary fittings to convert the fuse unit into a disconnecting unit. Figure 2 shows this assembly in the closed position.

The Fuse Unit

The new high-interrupting-capacity fuse unit consists of a short low-temperature calibrated element at the end of a solid conductor rod extending through a boric-acid-lined chamber, a copper tube for conducting current from the upper ferrule to the arcing rod through a modified form of tulip contacts and enclosing the operating spring which withdraws the arcing rod from the interrupting chamber, all enclosed in a lightweight Micarta tube with copper ferrules as shown in Figure 3.

The insulating tube is made from a rolled Micarta tube selected for high mechanical and impact strength, high dielectric strength, resistance to moisture absorption, and freedom from warping. The electrostatic shield at the lower end of the tube consists of copper foil rolled into the tube during fabrication in the same manner as used in the construction of high-voltage condenser bushings. This foil is electrically connected to the lower ferrule of the fuse unit. Copper ferrules are shrunk onto the fuse tube and then rolled.

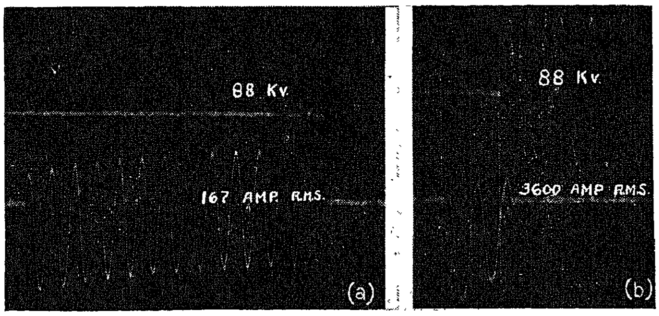
The lower half of the fuse tube is lined with molded boric-acid blocks cemented in the tube with a moisture-resistant high-dielectric-strength cement. The diameter of the opening in the boric acid is a maximum at the bottom of the interrupting chamber tapering down to a diameter slightly larger than the size of the arcing rod at the top. The length of boric acid having maximum opening is chosen so that interruption of the maximum fault currents will take place when the arcing rod has traveled a distance sufficient to prevent severe voltage stress. Moderate fault currents are interrupted when the arcing rod has traversed the intermediate taper, and low currents are cleared when the arcing rod is drawn into the final taper. To insure that low fault currents corresponding to the minimum melting current of the lower rated fuse elements are cleared in the shortest possible arcing time, the arcing rod and the bore of the top section of boric acid are of smaller diameter than that of the rod and boric-acid blocks used for the high current ratings. The minimum current to be interrupted by a fuse will be, of course, dependent on its ampere rating.

An enlarged section at the top end of the arcing rod engages the flexible contact fingers which are part of the conductor tube to the top fuse-tube ferrule.

Figure 8. Oscillograms of short-circuit tests on 92-kv fuse

(a). 167 amperes, 88-kv restored voltage

(b). 3,600 amperes, 88-kv restored voltage



The contact surfaces are heavily silver-plated for low contact drop for the life of the fuse under all atmospheric and temperature conditions. A stainless-steel compression spring assures uniform and continuous contact pressure without binding. These contacts are so biased that the effort required to withdraw the contact from the fingers is only a small fraction of that exerted by the stainless-steel operating spring which is terminated on this enlarged section of contact rod.

The calibrated element in this new fuse is of the low-temperature type so that operation at or near full-load rating will not create temperatures that will dehydrate the boric-acid lining or impair the strength of the fuse tube. A high-strength strain element relieves the calibrated element of all spring tension.

When the calibrated element is fused by a fault current, the operating spring draws the arcing rod through the boric-acid arcing chamber. The heat of the arc decomposes the surface of the boric acid into water vapor and inert boric oxide. The blast of water vapor deionizes the arc path and effects interruption. The arcing rod, which is not rigidly connected to the operating spring does not stop when the spring reaches its fully closed position but continues on through the spring by virtue of its inertia until the end punctures a frangible disk in the top ferrule. A special spring washer stops the travel of the arcing rod by arresting its enlarged section. This washer also functions to retain the arcing rod and prevent it from dropping back into the tube. The rod projecting from the top ferrule initiates the drop-out mechanism. It is obvious that the drop-out action cannot start until the arcing rod has traversed the entire length of the interrupting chamber which insures that all fault currents are interrupted before the tripping out action occurs.

The Fuse Mounting

The fuse-unit fittings which comprise

Figure 9. Oscillograms of short-circuit tests on 115-kv fuse

(a). 145 amperes, 110-kv restored voltage

b). 4,400 amperes, 110-kv restored voltage

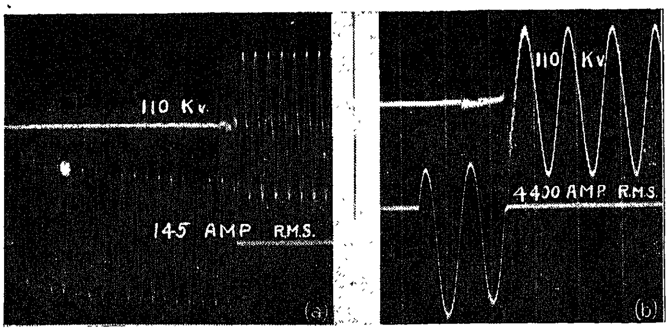
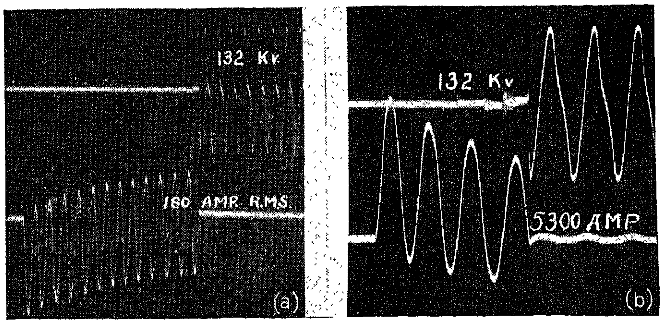


Figure 10. Oscillograms of short-circuit tests on 138-kv fuse

(a). 180 amperes, 132-kv restored voltage

(b). 5,300 amperes, 132-kv restored voltage



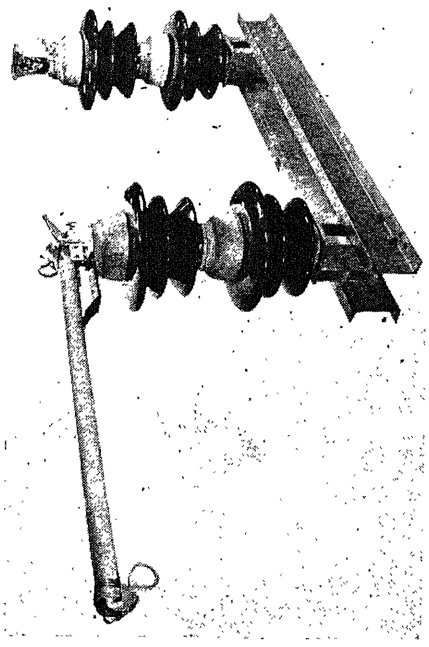


Figure 11. Single-pole unit, 92 kv, 200 amperes, with fuse. Unit in dropout position

a part of the fuse mounting are shown clamped onto the fuse tube in Figure 4. These fittings, comprising a hook eye for the top end of the fuse unit so that the fuse unit can be used as a disconnecting switch and a hinge casting for the lower end of the fuse tube that incorporates a lifting eye to facilitate the removal and replacement of the fuse with a standard hook stick, are readily removed from a blown fuse unit and placed on a new unit. These fittings are so keyed to the fuse unit that the correct fitting can fit only on its end of the fuse unit and assures perfect alignment of the fittings.

The fuse mounting proper consists of a double steel channel base for the higher voltages, and two insulator columns with latch castings and hinge castings mounted thereon. The top contact and trip-out mechanism are enclosed in a sleetproof housing, as shown in Figure 5. Current is conducted from the terminal to the pivoted latch casting by means of a flexible copper shunt. A powerful stainless-steel compression spring insures adequate contact between the latch casting and the hookeye casting clamped to the top ferrule of the fuse unit. It will be noticed that the force exerted by this spring puts the fuse tube in compression and thus avoids warping of the fuse tube from continuous cantilever stress. The drop-out trigger is pivotally mounted on the latch casting and bears on a projection within the sleet hood. Tests have shown that the kinetic energy of the moving arcing rod is more than ample to insure positive tripping regardless of alignment, variations in length of the fuse unit, or any possible friction that might be due to weathering or corrosive atmospheres.

The construction of the hinge is shown in Figure 6. Current is conducted from the fuse unit to the terminal pads by copper alloy leaf springs. The lower or lifting eye casting on the fuse unit and the hinge casting are so designed that the fuse unit is positively guided throughout the disconnecting or closing-in movements. The fuse unit can be removed from the mounting only when the fuse unit is in the 180°-open position. This arrangement also provides for the lower insulator stack taking the entire upward reaction resulting from gas blast on high current interruption thus insuring proper action of the drop-out mechanism. Below the hinge casting is a simple leaf spring bumper to absorb the energy of the falling fuse unit without overstressing the insulator column. This arrangement was chosen in preference to a friction brake because of its simplicity, its reliability under all atmospheric conditions, and the amount of energy to be absorbed in braking the fall of the long fuse unit.

Test Results

Complete interrupting tests have been made in the high-power laboratory covering the range from the minimum melting current of the smallest fusible element up to destruction of the fuse. Test-circuit conditions were modified from a very severe circuit where voltage-recovery rates correspond to a natural frequency of approximately 7,000 cycles per second to circuits with a very low voltage-recovery rate corresponding to natural frequencies as low as 800 cycles per second.

The majority of the interrupting tests were made with full line-to-line voltage impressed across a single fuse unit with the lower terminal at ground potential. Other tests were made with line-to-line voltage across a single fuse with the neutral grounded at the fuse mounting base. Also the fuses have been tested at line-to-neutral voltages to determine the upper current limit at reduced voltage.

Representative oscillograms of interrupting tests are shown in Figures 7 to 10.

The new assembly for carrying current through the fuse unit has been thoroughly tested for reliability on all values of fault current up to those which cause destruction of the fuse unit. In addition, high-current short-duration faults that did not blow the largest fuse rating were applied. In no case was there a failure of these parts to perform satisfactorily. In the case of the high currents applied momentarily there was no welding or damage that might cause failure to operate on subsequent faults. Prolonged temperature-rise tests indicate the complete adequacy of all current-carrying parts and contacts. The silvered contacts on the fuse rod within the fuse unit carried full rated current with an unusually low temperature rise.

High-speed moving pictures were used extensively during interrupting tests as a check on the operating sequence of the drop-out action and for checking the time required to trip out. These moving pictures were of invaluable assistance in locating the cause of flashovers in the early designs. They also presented an accurate picture of the size and shape of the cone of gases evolved from the fuse, thus enabling the designer to design an opening so that gases would not be deflected in such a way as to cause flashover of adjacent apparatus.

Application

The selection of a high-voltage fuse for isolation of faults in potential transformers does not require any special precautions, as the fuse can operate only on a fault in the connected equipment. When high-voltage transmission system is tapped to obtain small blocks of power, it is recommended that high-voltage fuses be connected as close to the power transformer as practicable and that sec

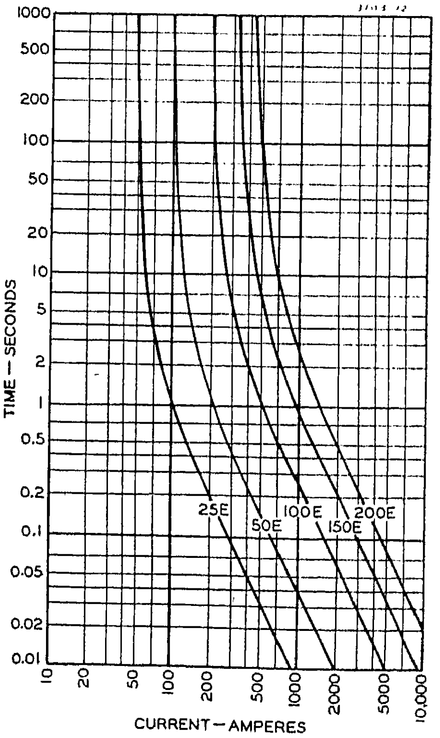


Figure 12. Melting time—current curves for representative fuse ratings

Subcenter Switching Systems for Teleprinter Tie Lines

F. L. CURRIE
MEMBER AIEE

Synopsis: One of the means used by the telegraph companies for improving the speed of their service is the provision of printing telegraph circuits between their central offices and the offices of many users of the telegraph. In the beginning, circuits of that type were restricted to patrons whose volume of telegraph business and proximity to a central office justified the expense involved in their installation and maintenance. A large part of that expense, when the patrons are located remotely from the central office, is in the line-wire costs.

This paper describes an automatic switching means for economically enabling a large number of patrons, when grouped in one locality, to be furnished printing telegraph service with a distant central office by causing them to share the use of a comparatively small number of line wires between that locality and the distant central office.

This paper also cites benefits gained from present installations.

AMONG the methods used by the telegraph companies during the last two decades for improving the speed of their service has been the provision of printing telegraph circuits between their central offices and the offices of many users of the telegraph service. That type of accommodation employes the use of the printing telegraph mechanisms, variously termed "teleprinters," "teletypewriters," or "teletypes," and is known in the Western Union Telegraph Company as teleprinter tie-line service.

Because of the expense involved in providing and maintaining the equipment and line wires essential to that service, it has been restricted to those patrons whose proximity to the central office and volume of business warranted the expenditure. Therefore, many substantial users of the telegraph service, because of their remoteness from a central office, had to be denied the advantages of teleprinter tie-line service because of the high line-wire costs involved. In some cases even groups of patrons were in that category.

The Western Union Telegraph Company undertook several years ago the development of a means for bringing those groups of remote patrons within the scope of teleprinter tie-line service by reducing the amount of line wire required.

Long experience in serving patrons over teleprinter tie-lines through central-office concentrators[1] has shown that most of those lines are in use only a small part of the time, and that the probability of any large percentage being used at any one time is small. That knowledge of their normal requirements led to the development of a remote controlled switching means for enabling a large number of patrons, grouped in one locality, to share the use of a comparatively small number of line wires to a distant central office. In that manner the necessary line-wire savings are effected to make economically feasible the extension to many more patrons the faster service afforded by teleprinter tie-lines.

The switching means, termed "subcenter," is designed to be installed in the same locality as the group of patrons, to be connected to each patron's office by an individual line wire, termed "patron's line," and to be connected to the central office by only as many line wires, termed "trunks," as the peak volume of business at any one time requires. An installation containing a subcenter is termed a "subcenter switching system."

Description

EQUIPMENT

The equipment required for a subcenter switching system is divided into

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ondary circuit breakers or other protective devices be used to interrupt all secondary faults. The high-voltage fuses will then operate only on faults in the power transformers. For such applications the fuse rating selected must provide adequate time for co-ordination with the relays controlling the secondary breaker. The proper rating should be determined from the time-current characteristic curves of the fuses and will usually exceed four times the full-load current of the transformer.

The addition of fuses on a system to isolate faults in equipment will enhance the reliability of the entire system by the rapid interruption of short circuits that would otherwise result in breaker operations or even in ground that might be difficult to locate.

At the higher transmission voltages it is generally not advisable to use fuses to protect even a small tap line that is exposed to lightning, unless the nature of the load is such that an outage caused by fuse operation can be tolerated.

Conclusions

Numerous interrupting tests, covering the complete range of fault currents under all possible circuit conditions, show that these new fuses are entirely satisfactory for application to important high-voltage transmission circuits. Their interrupting ability is such that they may be safely applied to systems approximating 1,000,000-kva short-circuit capacity at 66 kv through 132 kv.

The positive time-delay drop-out action provides an air gap in the circuit after operation which has a breakdown voltage ten per cent greater than the breakdown voltage of the basic impulse level (BIL) insulator columns. These fuses may therefore be applied on hydroelectric transmission circuits where sudden interruption of load currents may cause overshoot of recovery voltages.

These new high-interrupting-capacity power fuses can now be applied to important high-voltage transmission systems to rapidly clear faults in connected equipment, thereby enhancing the reliability of the complete system. They also provide an economical means for tapping off small blocks of power from high-capacity systems.

References

1. THE BORIC-ACID FUSE, A. P. Strom, H. L. Rawlins. AIEE TRANSACTIONS, volume 51, 1932 pages 1020–5.

2. HIGH-VOLTAGE FUSING OF TRANSFORMER BANKS, H. H. Marsh, G. B. Dodds. AIEE TRANSACTIONS, volume 61, 1942, July section, pages 533–5.

3. SYSTEM SHORT-CIRCUIT CURRENTS, W. M. Hanna, H. A. Travers, C. F. Wagner, C. A. Woodrow, W. F. Skeats. AIEE TRANSACTIONS, volume 60, 1941, September section, pages 877–81.

three general groupings, one for the subcenter, one for the central office, and one for the patron offices.

SUBCENTER

The subcenter switching equipment consists primarily of electrically operated multicontact relays and multicontact switches mounted either on racks or in cabinets, and in their assembled form termed "subcenter switching units." The relays are used primarily for control and signaling purposes, and the switches for making connections between the trunks and the patron tie-lines.

The switches are of the well-known Strowger type of two-motion and rotary switches. The two-motion switch is used to select the desired patron's line wire when a call is originated at the central office, and the rotary switch is used to seek the patron line wire when a call is made from the patron's office. The corresponding contacts of the contact banks which form a part of the two-motion and rotary switches are connected in multiple so that all of the patron tie-lines are available to all of the switches. The wipers of each switch are associated with a group of equipment individual to one trunk and complete the connections between that equipment and the equipment associated with the patron's line.

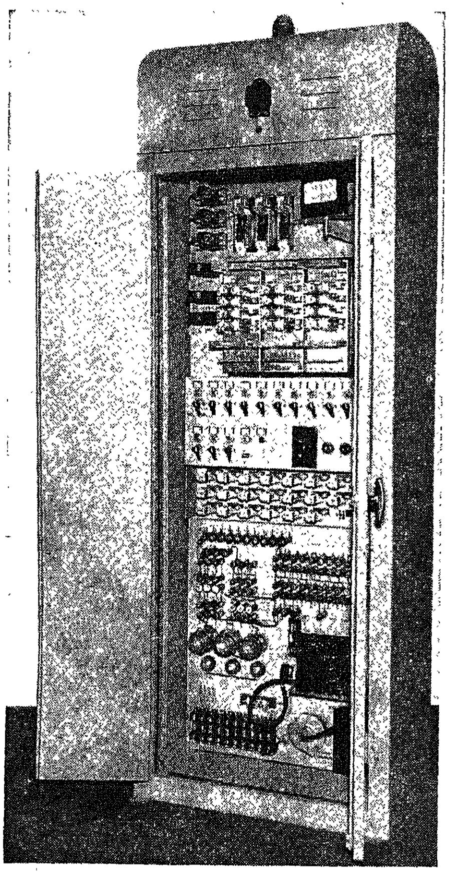

Figure 1. Small-size subcenter switching unit for ten patron tie lines and three central-office trunks

Three types of trunks, termed answering, calling, and combination trunks, are used for connecting the subcenter to the central office. The "answering trunks" are the trunks over which connections are made only when the calls are originated at patron offices; the "calling trunks," only when the calls are made at the central office; and the "combination trunks," when the calls are made at either the patron offices or the central office. The combination trunks, with the exception of the smallest size installations, require as much equipment in the subcenter switching units as an answering and a calling trunk combined, and, therefore, but few of them are used in any one installation. When installed, they are included primarily as overflow trunks, since they function as either answering or calling trunks.

CENTRAL OFFICE

The central office equipment may consist either of a group of operating tables, termed "subcenter operating tables," with each table connected directly to a trunk, or of existing teleprinter concentrator equipment.

The subcenter operating tables are classified as "answering," "calling," or "combination," the classification depending on the type of trunk with which they are to be used. The equipment required is basically the same for all of them. In general, it includes a teleprinter, a signal lamp, control relays, and in the case of a calling or combination trunk, a dialing unit. The teleprinters, signal lamps, and dialing units are mounted on the table tops and the control relays in a metal box underneath.

The subcenters also are designed to direct the calls of all patron offices to the central office over as few trunks as possible by always switching the patron lines to the lowest numbered idle trunk. That feature enables them also to perform the functions of an automatic concentrator for the central office. Although they function as concentrators themselves their central-office trunks are often connected to concentrators at the central office for converging the business of one subcenter with that of another, and also with that of the patron tie-lines connected directly to the concentrator.

The equipment of teleprinter concentrators consists, essentially, of turrets, control relays, signaling lamps, and operating tables. Each turret contains a large number of jacks, and one is located convenient to each operating table. Each turret jack is multipled to the corresponding jack of all of the other turrets, and each of the multiple connections is connected to a separate incoming line which may be either a teleprinter tie-line or an answering trunk from a subcenter. Thus, all of the directly connected patron tie-lines and the answering trunks from the subcenter are connected to all of the turrets so that any line or trunk can be worked from any turret.

Each operating table of the concentrator contains, primarily, a teleprinter and a circuit from the teleprinter through control relays to a cord and plug located at a turret. As many operating tables are provided as are likely to be required at any one time. Connection is made between an operating table and a desired patron's line or answering trunk from a subcenter by inserting the plug of that operating table in the proper jack of the turret.

Each of the calling trunks from a subcenter is connected directly to one of the operating tables of the concentrator and can be worked only from that operating table. A dialing unit containing a dial, similar in general appearance to the dials used in automatic telephone systems, and a control key switch, is added to the equipment and circuits which normally form a part of those operating tables. The key switch is used for adapting the operating table to work with either its directly connected calling trunk from a subcenter or with any line connected to the turret jacks, and the dialing unit is used to select the line of any of the patron offices connected to the subcenter by controlling the selecting mechanism of that equipment.

Each combination trunk from a subcenter is connected both to a multipled jack circuit in the turrets and to one of the operating tables, since it functions as either an answering or calling trunk.

When the combination trunk is in use as an answering trunk, it is automatically disconnected from the operating table to which it is normally connected, and, when it is used as a calling trunk, it is automatically disconnected from the turrets. Also, when it is used as a calling trunk, the equipment associated with it at the subcenter automatically busies it to all patron calls. Hence, safeguards are included to prevent interference to service from any attempt to use this circuit for its two functions at the same time.

PATRON'S OFFICE

The equipment at the patron's office consists, primarily, of a teleprinter, a signal lamp, a polar relay, and a teleprinter operating table. That is the same teleprinter tie-line equipment which is used for operation with concentrators. The signal lamp is controlled by the polar relay and serves to advise the operator when the central office calls.

STANDARD SYSTEMS

Each of the first subcenter switching systems was devised for a specific installation, but the later ones were designed as standard systems for general usage. The latter differ from the first in flexibility, in the ultimate sizes of the installations, and in some details of circuit design. They were designed for three different sizes of installations, small, intermediate, and large, so that an economically feasible one would be available for any probable requirement.

SMALL SYSTEM

The smallest of the systems enables groups of ten patrons to be served by a remote central office over three trunks. The subcenter switching equipment is housed in a single cabinet as illustrated in Figure 1.

Combination trunks, only, are provided between the central office and the subcenter, and the same equipment at the subcenter is used for calls originating at either the central or the patron's office. This differs from the intermediate and large systems, described later, in that those systems require two sets of equipment at the subcenter, one calling and one answering, for each combination trunk.

Provision is made in the design of the units for connecting two of them together in a single installation to provide for 20 patrons and six trunks when the requirements grow beyond the capacity of one unit.

INTERMEDIATE SYSTEM

The next, or intermediate, size system is designed to serve groups of 25 patrons over eight trunks to the central office. The equipment of the subcenter, like that of the small system, is housed in a single cabinet (Figure 2).

Normally, three calling and five answering trunks are provided between the subcenter and the central office. Combination trunks may be provided by connecting together, at the subcenter, the equipment of one calling and of one answering trunk for each combination trunk desired. Hence, each combination trunk provided reduces by one the total number of central-office trunks that can be accommodated by the subcenter unit. A maximum of three combination trunks may be provided in this manner for each unit.

Provision also is made in the design of the subcenter switching units of this system for connecting two of them together in a single installation to double the capacity to 50 patrons, six calling and ten answering trunks.

LARGE SYSTEM

Each of the small and intermediate systems requires that all of the patrons be connected to the one subcenter in order to share the trunks to the central office. The large system, in addition to serving a much larger number of patrons, also provides for serving the trunks of as many as seven smaller size subcenters. Thus, the large system may consist of one main subcenter serving 200 patron tie-lines and as many as seven tributary subcenters, each of which may serve 25 patron tie-lines, combined into a single large system serving a total of 375 patrons. The main subcenter, to which the central-office trunks are terminated, is termed the "subcenter," while the seven tributary subcenters are termed "sub-subcenters." The sub-subcenters may be either the 10 or the 25 patron size already described. Combining a large number of patron tie-lines in one system in that manner considerably reduces the number of trunks that would be required between the locality of the patrons and the distant central office if those patron tie-lines were served by several smaller systems. The plan of this system is illustrated by the diagram of Figure 3.

The equipment of the subcenter is assembled in nine units (see Figure 3). Two of the units contain that portion associated directly with the calling trunks; two, that portion associated directly with the answering trunks; one, that portion associated directly with the sub-subcenter answering trunks; and, four, that portion associated with the directly connected patrons. Each calling trunk unit provides for eight calling trunks to the central office and ten sub-subcenter calling trunks; each answering trunk unit, for 16 answering trunks to the central office; each patron tie-line unit, for 50 patron tie-lines; and, the sub-subcenter answering trunk unit for 35 sub-subcenter answering trunks. Hence, an installation equipped for maximum capacity operation provides for 16 central-office calling trunks, 32 central-office answering trunks, 200 directly connected patron tie-lines, and 55 sub-subcenter trunks.

All of the patrons, regardless of whether they are connected to the subcenter directly or by sub-subcenter trunks, share the use of all of the central-office trunks.

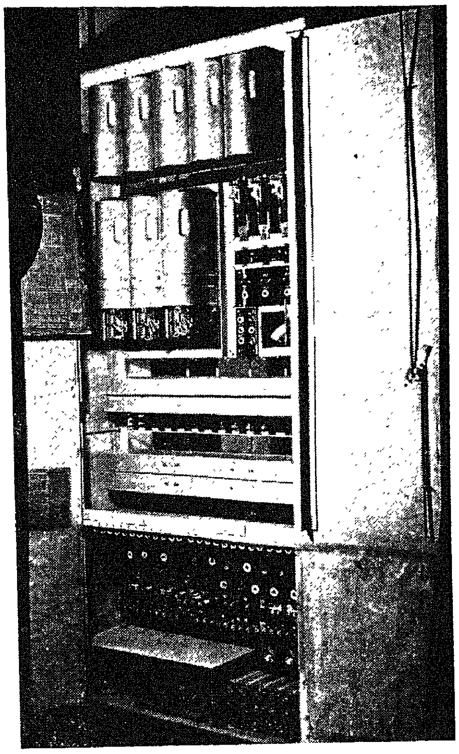

Figure 2. Intermediate-size subcenter switching unit for 25 patron tie lines and eight central-office trunks

The trunks between the subcenter and the central office also may be converted to combination trunks by combining the equipment in the subcenter of one calling and of one answering trunk for each combination trunk desired. A maximum of 16 combination trunks can be obtained in that manner.

Operation

All of the subcenter switching systems mentioned in this paper operate fundamentally in the same manner.

Calls can be originated from either the central or any patron's office, but connections can be completed only between the central and a patron's office. No provision is made for making connections between any two patron offices.

After a connection is made, transmission may be carried on in either direction until the connection is released, regardless of which office initiated the call. The connections normally are released only from the central office.

The operations required at either the central or the patrons' offices for causing the subcenter switching equipment to effect a connection are so simple that no special training of the personnel is necessary for proper operation.

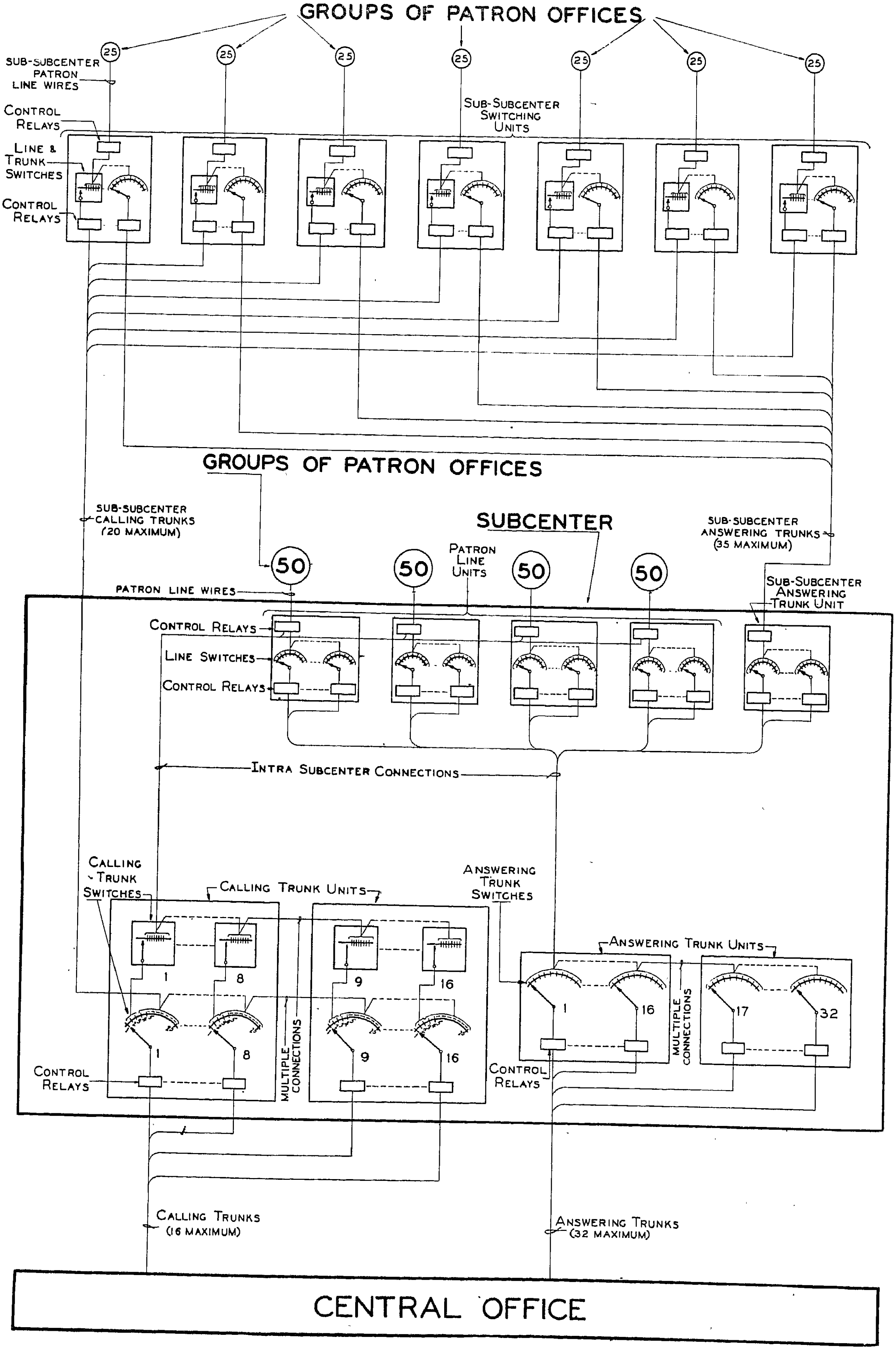

Figure 3. Diagram showing plan of large subcenter switching system

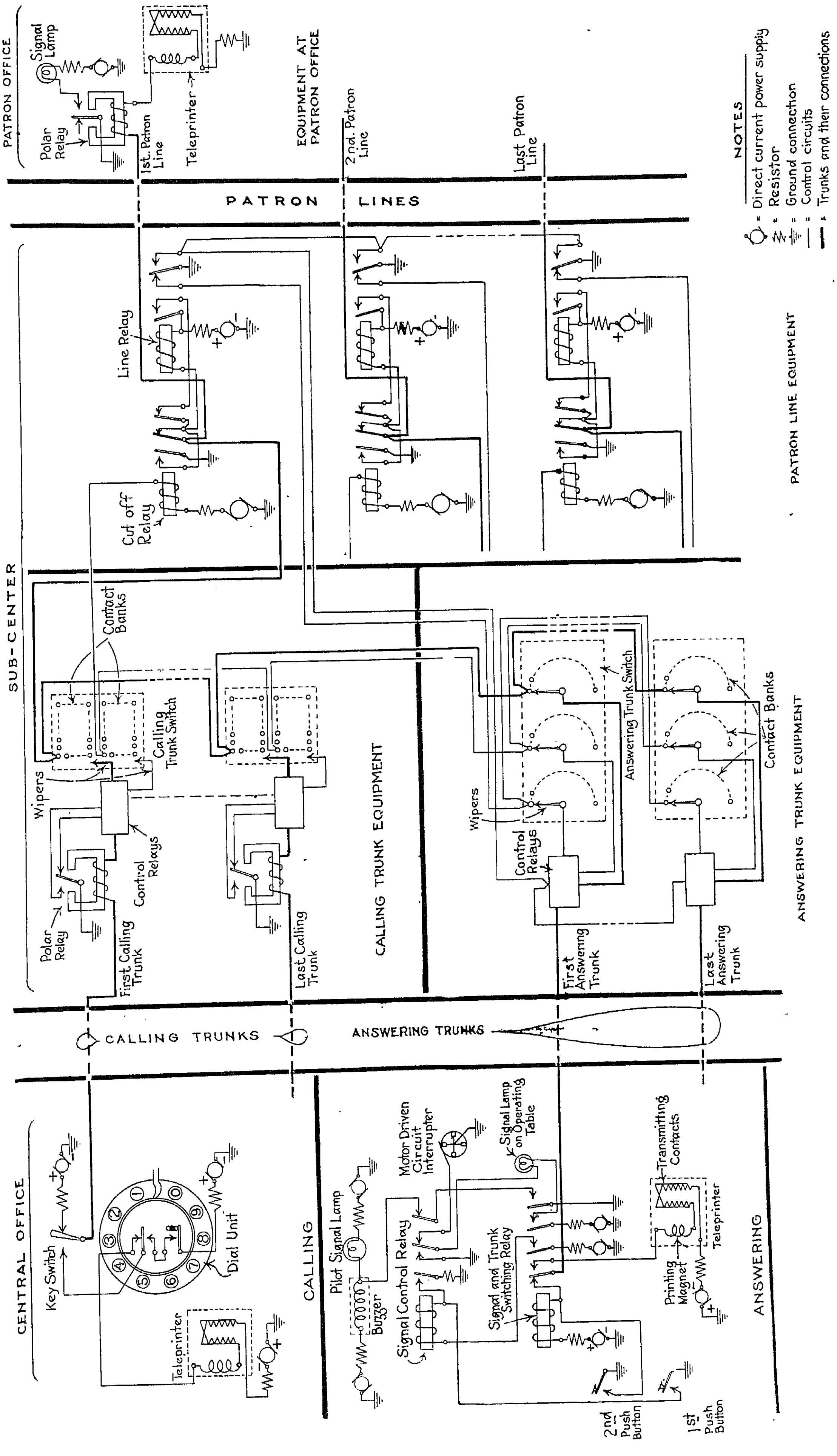

Figure 4. Simplified schematic diagram of the circuit and equipment used in a typical subcenter switching system

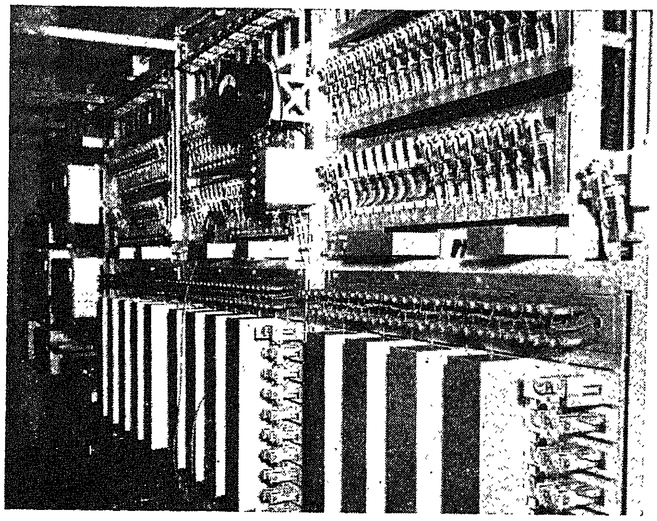

Figure 5. View of switching equipment at Newark for large-size subcenter-switching system

Figure 4 is a simplified schematic diagram of the circuit and equipment arrangements used in a typical subcenter switching system. The trunks are shown terminated on individual tables in the central office. Many details of the control circuits and relays are omitted from the diagram for reasons of simplicity.

A call originated at any patron's office is initiated by the patron's operator opening, momentarily, the normally closed line between her office and the subcenter by depressing any one of the keys of her teleprinter keyboard. The opening of the line allows a normally operated relay, termed "line relay," connected in series with the line in the subcenter switching unit, to release and lock itself released.

The release of that relay causes the wiper contacts of the switch associated with the lowest numbered idle answering trunk, through the medium of control circuits and relays, to seek the patrons' line containing that relay by advancing, step by step, over the contacts of its contact bank.

When that patron's connections are found, control circuits and relays associated with the respective switch and the patron's line function to disconnect ground from the trunk, and power from the patron's line, and after a brief lapse of time (about 0.3 second), to connect the trunk and the patron's line together. The brief interval of time between the removal of ground from the trunk and the connecting of the trunk to the patron's line wire constitutes a sufficient opening of the trunk to allow a normally operated relay whose winding is connected in series with the trunk at the central office to release and operate the central office signaling equipment.

When the answering trunks are connected directly to individual tables at the central office, the release of the relay, termed a "signaling and line switching relay," not only signals the operator but also connects the central-office teleprinter to the trunk and reverses the polarity of the power applied to the trunk from positive to negative.

Both visual and audible signals are provided at the central office. A pilot lamp and buzzer, conspicuously located, give a continuous alarm, and a signal lamp, mounted on the table to which the call is directed, flashes repeatedly until an operator presses a push button on the table top for operating the signal control relay. Those signals advise the operators that a new call has been received, and the push button is pressed when the call is answered. The operation of the signal control relay silences the buzzer, extinguishes the pilot lamp, and causes the signal lamp on the table to glow steadily, instead of flashing, and to continue glowing as long as the connection is allowed to remain. This alarm signaling arrangement is used to insure that all calls are given prompt attention, and the continuous glowing of the signal lamp is provided for supervisory purposes.

The reversal of the power applied to the trunk, from positive to negative, causes a polar relay, connected in series with the trunk at the subcenter, to reverse its armature and prepare the subcenter switching equipment for releasing the connection when the transmission of business is completed.

When the transmission is completed, the central-office operator presses a second push button, which reoperates the signaling and line switching relay. The operation of that relay transfers the trunk from the teleprinter and power of negative polarity to a circuit through its own winding and power of positive polarity. It also opens the circuits to the signal lamp and the signal control relay winding causing the lamp to be extinguished and the relay to release. The reversal of power to the trunk from negative to positive causes the armature of the polar relay, whose winding is connected in series with the trunk in the subcenter, to reverse to its original position and release the connection. The trunk is then available for connection to another patron's line wire.

When the answering trunks are terminated at a teleprinter concentrator, the opening of the trunk at the subcenter causes only a signal to be given at the central office for identifying the trunk and attracting the attention of the operator. The reversal of power polarity to the subcenter, from positive to negative, is effected by the operator's inserting the plug of her operating table circuit into the proper jack of the turret. This operation transfers the trunk from its normal idle connection and positive polarity of power to the operating table circuit and negative power.

When transmission is completed, the plug is removed from the jack, causing the power applied to the trunk to be changed to positive, which is the idle condition

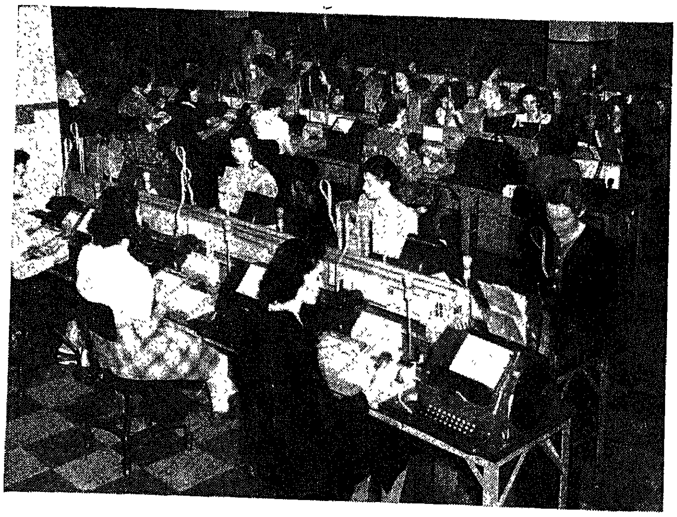

Figure 6. Section of the Western Union central office in New York. This view shows some of the subcenter operating tables used with the Newark subcenter switching system

polarity. Hence, the plug and jack, with their associated relays, perform the same functions at the central office when concentrators are used as the signaling and trunk switching relay and the push buttons when individual tables are used.

Calls are initiated at the central office by reversing the power applied to the trunk from positive to negative by operating a key switch and by dialing the numeral assigned to the desired patron office in the same manner as the digits of dial telephone numbers are dialed. The reversal of power polarity prepares the subcenter equipment, associated with the calling trunk, for responding to the dial pulses and for advancing the wiper contacts of the switch to the contacts of its bank associated with the desired patron's line.

When the proper contacts are reached, the patron's line is first tested automatically at the subcenter to determine whether it is idle or is busy. If it is busy, an automatic busy-signal transmitting device sends a busy signal over the calling trunk to the central-office operator. If it is idle, control relays at the subcenter associated with the trunk and the dialed patron's line function to disconnect the patron's line from positive power at the subcenter and connect it to the trunk line which, at that time, has power of negative polarity applied at the central office. This reverses the direction of current over the patron's line and causes a polar relay connected in series with the line at the patron's office to operate and signal the call. The patron's operator answers the call with her teleprinter keyboard.

When transmission is completed, the central-office operator restores the lever of her key switch to its normal idle position which restores the central-office circuits and equipment to their idle condition, and reverses the polarity of the power applied to the trunk for releasing the connection at the subcenter. The trunk then is available for connection to another patron's line.

Several interesting auxiliary features are contained in the switching units. One of them is a means for automatically wiping out false calls on patron's lines which might result in the event of a temporary interruption of the local power supply to the subcenter switching units. Since power is furnished the patron line wires from the subcenter switching units, any interruption of that power results in an interruption of the current flow over the line wires. Any interruption of the current flow over the patron's line wires allows the line relays of all the patron lines at the subcenter to release in the same manner as they do for bona fide patron calls. If automatic preventive measures were not included in the design, each patron line wire would be switched to the central office as fast as answering trunks became available until all had been connected to and released by the central office. The false call wipe-out feature functions after each power failure to prevent all switches associated with answering trunks from operating, to re-establish the patron line-wire relays at the subcenter to their normal idle operated condition, and then to allow all switches to operate normally again.

Another interesting feature is a provision for "busying" the equipment at the subcenter of any answering trunk to all new calls by opening the answering trunk circuit. That enables the central-office attendants to remove any trunk from service for maintenance or for any other reason, and it also automatically prevents the connecting of a patron's line to an open trunk.

Installations

Examples of the benefits which subcenter switching systems can render are afforded by the installations which have been in service for several years between patrons in Oakland and the central office in San Francisco, patrons in Beverly Hills and the central office in Los Angeles, and patrons in Newark and the central office in New York City.

The Oakland–San Francisco system was designed to enable the central office in San Francisco to serve 50 patrons located in Oakland over 16 submarine cable conductors (six calling and ten answering trunks) across San Francisco Bay. All trunks were terminated in an existing teleprinter concentrator at the central office in San Francisco.

The Beverly Hills–Los Angeles system utilizes one of the 25 patron subcenter switching units previously described and shown in Figure 2. All trunks are terminated in an existing teleprinter concentrator at the central office in Los Angeles. This installation relieved an acute wire shortage caused by furnishing direct teleprinter tie-line service between those points to 15 patrons, and at the same time provided accommodations for extending the service to ten more such patrons.

The Newark–New York City system was the first of the larger systems designed and installed. Though the installation as now equipped accommodates only 150 patrons, the design provides for adding equipment to increase this number to 200 whenever the need for it develops.

The equipment of the subcenter, which is located in Newark, is housed in five racks as shown in Figure 5, and these racks are enclosed in an air-conditioned room for protecting the equipment from dust. The three similar racks shown in the foreground are the patron line units, the fourth is the calling trunk unit, and the fifth is the answering trunk unit. One patron line unit has a capacity of 50 patron tie-lines; one calling trunk unit, ten calling trunks; and one answering trunk unit, 36 answering trunks.

The system, at present, is served by 27 trunks, two of which are calling, five are combination, and 20 are answering. These trunks are terminated on individual tables in the central office in New York. Figure 6 is a view of that portion of the central office in New York containing most of those tables.

The first two installations, the Oakland–San Francisco and the Beverly Hills–Los Angeles, effected substantial savings in line wires and increased the capacity of the Western Union to furnish more of the same type of service between those points.

The Newark–New York installation greatly reduced the cost of operation by eliminating one relaying, or retransmission, of the patron business at Newark.

In the past, the business was received in the Newark central office and most of it was retransmitted to one of the few large central offices connected to it, for further retransmission to its destination.

At present that part of the business destined for New York is handled directly, and the rest is retransmitted either to its destination or to a point much closer than was possible before, since the New York office is provided with direct connections to many more cities than was the Newark office.

The benefits anticipated from the use of subcenter switching systems have been fully realized in the installations made thus far.

Ease of operation, improved speed of service, saving of line wires, and in some instances, such as the Newark case, the reduction of operating costs, are the factors which make this development promising for future use in the telegraph industry.

Reference

1. A Printing Telegraph Concentrator, W. B. Blanton. AIEE Transactions, volume 50, 1931, pages 414–25.

Aircraft Circuit Breakers

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FELLOW AIEE

Synopsis: Airplane circuit breakers are recognized as important units in the electrical systems of military aircraft. The design of such circuit breakers can be combined with switching and contactor functions. This makes possible the use of well-developed switch and contactor structures.

The design of such circuit breakers must provide for operation under the very exacting requirements of military aircraft service.

The principal function of both manual and remote-control circuit breakers is to provide short-circuit protection. There is some question as to the desirability of motor protection for military aircraft. Many motor functions are vital to the operation of the aircraft, and it may be better to operate the motor to destruction rather than to stop at a safe temperature or load value. Where motor overload protection is desired, it is necessary to correlate circuit-breaker and motor characteristics. Both manual and remote-control circuit breakers can be made to provide for holding the circuit closed in emergency to "force" the motor or other equipment.

ELECTRICAL operation and control have contributed largely to the effectiveness of today's military aircraft. It has been necessary to develop new types of electrical equipment to meet the special requirements of this service. Minimum size and weight are of extreme importance.[1] Control devices must operate in any position, must withstand high rates of acceleration and retardation, must operate in ambient temperatures of −50 to 200 degrees Fahrenheit, must operate at high altitude with rarefied atmosphere, and must withstand severe vibration.

Extensive electrical power systems are required for these military aircraft, particularly for the large bombers. Maximum reliability for the various circuits for these power systems is of unusual importance. Any part of the electrical power systems may be damaged in battle. As much of it as possible should remain available. It should be possible to restore power to any circuit as soon as a fault is cleared. Airplane-type circuit breakers are the logical answer to these problems.

Twenty-four volt d-c power with stand-by storage battery is the most extensively used power system for United States military aircraft. The lower voltage as compared to industrial power reduces the interrupting problem for circuit breakers but increases current values for a given power rating. Short-circuit faults in such a system may result in current of from 1,000 to 3,000 amperes. Such service presents serious contact problems.

These contact problems have been largely solved in the development of aircraft manual switches and contactors. One solution to the aircraft-circuit-breaker problem, therefore, is the addition of thermal trip units to these aircraft switches and contactor. Such an arrangement has a number of obvious advantages. The manual switches and contactors can be used for their normal service. The same mounting arrangements are available. Repairs and replacements are simplified. This type of design is a dual purpose one in which circuit or overload protection has been added to the normal switching or contactor function. Our discussion here will be limited to the features of circuit or overload protection.

The thermal trip units must be designed with special consideration for the service requirements of military aircraft. Their tripping values must not be appreciably affected by acceleration of 10g (where g is the acceleration of gravity) or more, or the effect of severe vibration. They must not change in rating in altitude from sea level to approximately 40,000 feet. These results can be accomplished by proper design.

Manual Circuit Breakers

The manual-type circuit breaker consisting of an airplane-type switch with bimetal thermal trip provides both circuit protection and switching functions. The X-ray pictures, Figures 1, 2, and 3, show the general construction of one such airplane circuit breaker. The switch mechanism provides make and break contacts operated by a toggle lever. This toggle lever may be provided with a luminous or fluorescent end. The trip unit is of the bimetal type. The bimetal latches a spring-actuated trip member. When the current through this bimetal trip unit exceeds a predetermined value for a sufficient length of time, the bimetal trips the latch and causes the circuit breaker to open its contacts. The contacts are reset by returning the toggle lever to the "on" position.

A thermal trip unit, in which the current flows through the bimetal, has a relatively sharp knee in the tripping-time curve. This is favorable to the protection of the circuit, as the wiring cable likewise has but little thermal capacity and reaches its maximum permissible temperature in a relatively short time. The relationship between the time re-

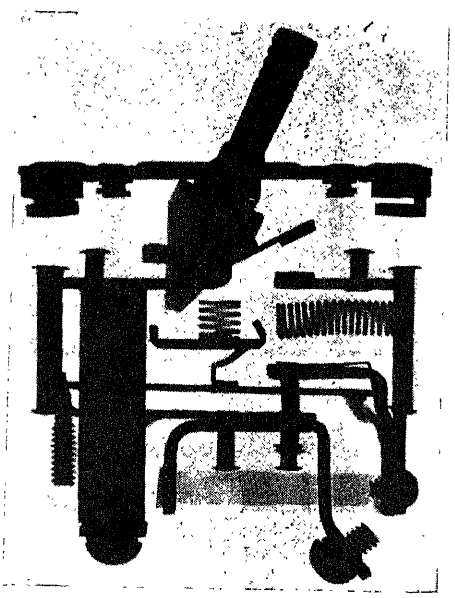

Figure 1. Manual circuit breaker
Contacts open, thermal trip unit latched

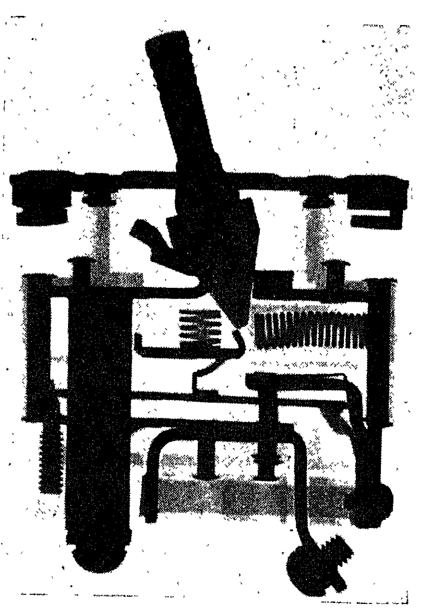

Figure 2. Manual circuit breaker
Contacts closed, thermal trip unit latched

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quired for the cable to reach a temperature of 90 degrees centigrade as compared to the tripping time of a circuit breaker with a tripping temperature of 150 degrees centigrade, is shown in Figure 4.

Short-circuit protection is the principal function of the manual circuit breaker. The amount of short-circuit current is determined by the circuit. High-capacity circuits will generally have higher short-circuit currents than low-capacity circuits. This is illustrated by the accompanying table which shows standard manual-circuit-breaker ratings and short-circuit test currents.

Table II of Army Navy Aeronautical Standard *AN-C-77*, February, 1943

Breaker Rating (Amperes)	Short-Circuit Load (Amperes)
5	1,000
10	1,000
15	1,500
20	1,500
25	2,000
35	2,500
50	3,000

The service requirement for aircraft circuit breakers differs in other particulars from those used in industry. Some of the applications make it desirable to "force" operation of the equipment even though the motors may seriously overheat. Specifications, therefore, require the nontrip-free type of circuit breaker. This is based on tripping of the circuit breaker to protect the circuit and equipment in case of faults, yet permitting the operator to "force" the motors, in case of emergency, by holding the circuit breaker in the closed position.

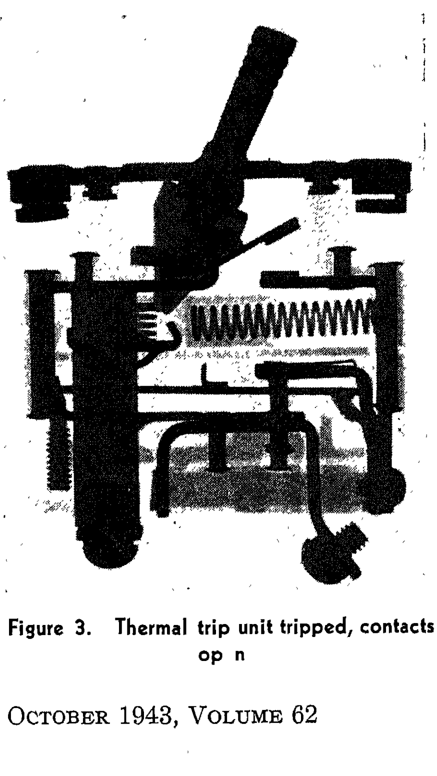

Figure 3. Thermal trip unit tripped, contacts open

Remote-Control Circuit Breakers

There are some applications, particularly on the larger military aircraft, where it is desirable to have the circuit breaker at a location remote from the point of control. Such installation reduces the amount of heavy wiring, thereby reducing the important element of weight. The reduced amount of heavy wiring further tends to increase reliability, because of damage from enemy fire.

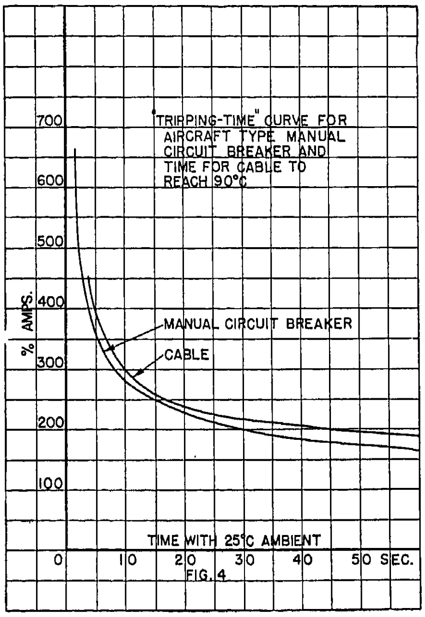

Figure 4

Circuit protection is the primary function of the remote-control circuit breaker. It is also possible to obtain motor protection, that is, to add the overload function to that of contactor and circuit breaker, making a triple-purpose device.

There is some question as to the desirability of motor protection for military aircraft. Many motor functions are vital to the operation of the aircraft, and it may be better to operate the motor to destruction rather than to stop at a safe temperature or load value. The following discussion, however, pertains to those applications where motor protection may be desired.

A remote-control circuit breaker consisting of an airplane-type contactor with thermal trip unit is shown in Figure 5. This combination can be obtained in the various standard sizes of contactor as used for airplane service, that is, 50-ampere, 100-ampere, and 200-ampere ratings. These airplane contactors provide the high interrupting capacity (3,000 amperes) and freedom from welding of contacts as required for circuit-breaker service.

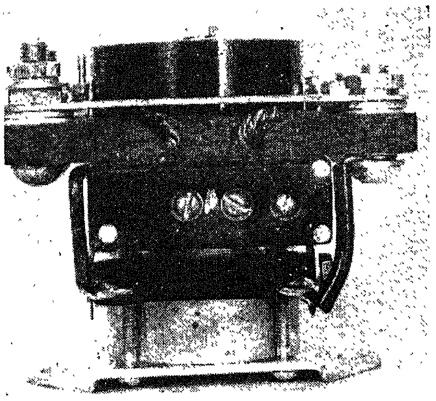

Figure 5. Remote-control circuit breaker

The thermal trip unit for these remote-control circuit breakers is shown in the X-ray photographs of Figures 6 and 7. This provides quick make and break pilot-duty contacts operated by a bimetal trip member. The contacts are insulated from the bimetal trip member by a small bakelite operating pin. The bimetal carries all or part of the main circuit current. The different ratings are obtained by the use of shunt elements which are an integral welded unit with the bimetal.

It is well established that the tripping temperature of an overload device should approximate the danger temperature of the equipment to be protected. For military aircraft it is desirable to operate to maximum capacity to avoid unnecessary weight. Class *A* insulation will permit of operation at temperatures of 125 degrees centigrade. This is recognized in the Underwriters' Laboratories standard on industrial control for built-in cycling overload motor protection.

The heating of any motor is, in general, proportional to the losses in the motor. It is, therefore, possible to determine an approximate heating curve for a motor if

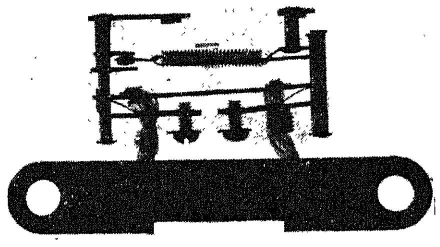

Figure 6. Remote-control circuit-breaker thermal trip unit

Contacts closed

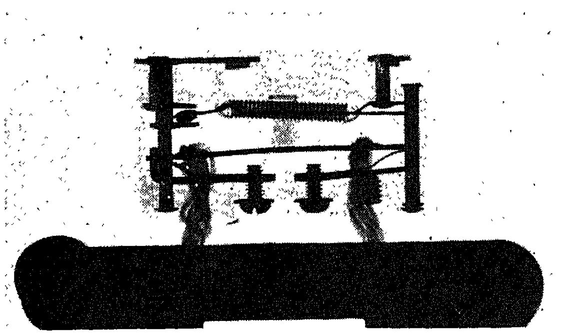

Figure 7. Remote-control circuit-breaker thermal trip unit

Contacts open

we have the efficiency curve and know the temperature rise at some particular loading. Such a study based on performance curves for a two-horsepower aircraft-type motor is shown in Figure 8.

From the heating curve for the motor we can readily determine a load curve for a range of ambient temperatures. This is based on the assumption that the final temperature of the motor will equal the sum of the ambient temperature and temperature rise for a given load. The proper trip temperature, for the thermal trip unit, is readily determined from this curve. Figure 9 shows motor load and thermal unit tripping-current curves. The motor load curve is based on a maximum winding temperature of 125 degrees centigrade and the curve for the thermal trip unit is based on trip temperature of 120 degrees centigrade.

The use of glass-insulated motor windings will of course permit of higher motor temperatures. Such motors require higher tripping temperature for overload protection. This is reflected to some extent in *AN-C-77* specifications which indicate an approximate tripping temperature of 155 degrees centigrade.

For motor protection it is necessary to provide a large number of ratings. These ratings should be spaced not more than ten per cent. If we protect the motor for a predetermined maximum temperature, it will be necessary to overprotect an amount equal to the spacing between ratings. This results in a corresponding loss in motor capacity.

Many aircraft motor applications are of the intermittent-duty type. The greatest need for motor protection on such application is protection against locked rotor. In order to determine such motor protection it is necessary to have the corresponding motor data. These include locked rotor current and the maximum time which the motor will permit of this current without exceeding the maximum permissible temperature. This maximum temperature may occur in either the field or armature windings. Many of these intermittent-duty applications are such that if the motor will operate at all, no further protection is needed. For such applications the remote-control circuit breaker provides motor protection equal to that which could be obtained with built-in overload protection. The remote-control circuit breaker provides quick reset for such applications, whereas built-in overload protection depends upon cooling of the motor and has a much longer reset time.

Circuit-breaker standards specify ratings which will cause tripping at 25 degrees centigrade from 115 per cent to 138 per cent of the established rating. This is primarily intended for circuit protection and to insure that rated load will not cause tripping. This method of rating must be understood when making applications of remote-control circuit breakers for motor protection. These ratings, however, can be readily translated into the usual overload ratings which provide that the overload unit trip under all conditions in a 40 degrees centigrade ambient. The recognized standard of accuracy for overload protection is a tolerance of +0 to −10 per cent.

Remote-control circuit breakers for many military aircraft applications should be arranged for "forcing" the motor in emergencies. It is also desirable to indicate tripping of a remote-control circuit breaker at the control point. These functions can be obtained by control circuits as shown on Figure 10. This circuit requires but a single control wire with grounded return. The use of the two-way momentary contact pilot switch provides six operating functions.

1. The contactor is closed by moving the pilot switch to the close position and is maintained through a control resistance which is mounted as a part of the remote-control circuit breaker.
2. The contacts are opened by moving the pilot switch to the open position.
3. The thermal trip unit contacts reclose automatically after tripping on overload, and operation to the close position re-establishes circuit to the load.
4. The motor can be forced against overload by holding the two-way momentary contact switch in close position.
5. The remote-control magnetic indicator is operated through the single control wire to show the position of the contactor.

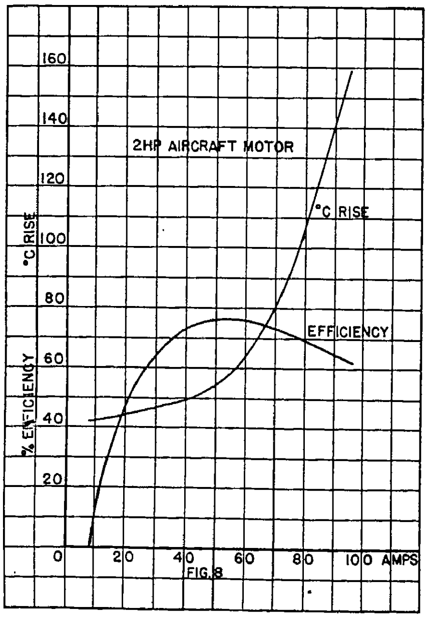

Figure 8

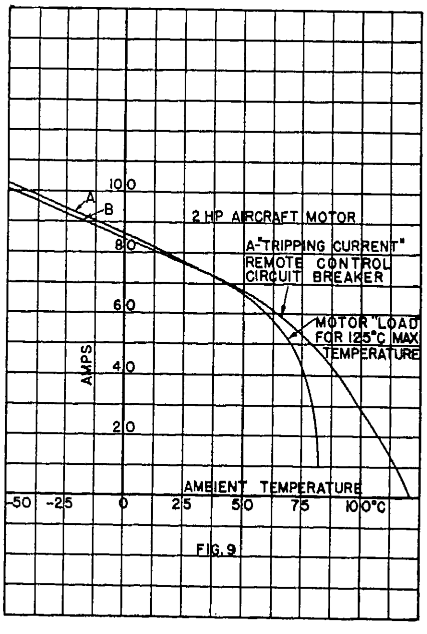

Figure 9

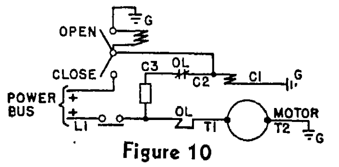

Figure 10

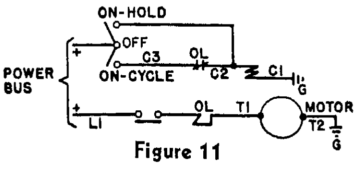

Figure 11

6. The contactor will maintain its last operating position, should the control wire be broken or shot away.

A second control scheme, Figure 11, provides operation of the remote-control contactor by means of a three-position pilot switch. The contactor can be held closed against motor overload if the pilot switch is in the "on-hold" position. The overload trip unit contacts reclose automatically, and, if the pilot switch be in the "on-cycle" position, the motor will restart automatically, after tripping on overload. If the control wire is shot away or broken, the contactor will open or remain open as the case may be.

Conclusions

Military aircraft have extensive electrical systems. Circuit breakers are an essential part of such an electrical system. Their use insures that as much of this system as possible will remain available when the aircraft is subjected to enemy fire. These circuit breakers can combine switch and contactor functions with that of circuit protection, thereby reducing weight.

Military aircraft service often requires the immediate and maximum operation of motors so that motor overload protection cannot be provided. For those applications, where motor-overload protection is desired, circuit-breaker characteristics must be properly related to motor characteristics. Some such applications are of very intermittent duty and stalled motor conditions is the principle consideration for motor protection. Both manual and remote-control circuit breakers can be of the nontrip-free type to permit the operator to "force" the motor or other circuit devices beyond conditions which cause tripping.

Reference

1. Application of Electric Power in Aircraft, T. B. Holliday. Electrical Engineering, volume 60, May 1941, pages 218–25.

Relay Protection of Tapped Transmission Lines

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Synopsis: The war, with its attendant shortage of critical materials, has led to a considerable increase in the number of tapped lines. This paper deals with the method of protecting circuits of this kind under the three headings:

1. Pilot-wire protection, a-c and d-c schemes.
2. Carrier-current protection.
3. Other protective schemes.
   Directional overcurrent or network.
   Distance type.

Three-terminal lines are classified into three types with regard to other system connections between the terminals connected by the line in question. These are further classified as to power sources to aid in analyzing limiting conditions for relay applications.

Typical operating characteristics of a-c pilot-wire equipment for three-terminal lines are shown.

In general in dealing with pilot-wire, carrier, and other types of protection, those problems peculiar to application on three-terminal lines are analyzed and methods given for solving some of the more troublesome problems.

Introduction

THE interconnection of several power supply and load points by a single tapped transmission line rather than by a number of two-terminal lines has always presented first cost advantages at some expense in reliability and flexibility. However, the relay protection of such lines has been somewhat of a nightmare to protection engineers. Increased operating times for complete clearing of faults, and even some loss of selectivity have had to be tolerated in many instances. It has been said that there are no good three-terminal lines; some are simply worse than others. While inaccurate, this thought does express the general sentiment of protection engineers toward such layouts.

Nevertheless, the war, with its attendant shortage of critical materials, and manpower for construction, has led to a considerable increase in the number of tapped lines and placed increased load requirements on others already in service. Many war plants, because of their locations and high power requirements have been provided with supply with a minimum use of critical materials by tapping the nearest high-voltage line or lines. The circuit breakers usually have been located on the low-voltage side of the transformer.

The present paper deals with the methods of protection for such lines under the headings of:

1. Directional overcurrent protection.
2. Impedance or distance-type protection.
3. Pilot-wire and carrier-pilot protection

It is hoped that by summarizing the existing schemes and presenting data on certain new schemes, particularly on pilot-wire protection, that some help can be given in the solution of this currently important problem. It is also hoped that this paper will elicit discussion which will bring to light other schemes which various protection engineers have devised to solve the problems here discussed. Such discussions will be of particular benefit to the industry at the present time.

Typical Tapped Lines

Tapped lines appear in all the categories of main transmission, subtransmission, and distribution. Two hydrogenerating stations may be connected together and to a load and steam generation area over a high-voltage three-terminal line of the main transmission class. The substations of an a-c electrified railway[2] are fed from dual 132-kv lines with as many as ten substation transformers tapped from each line. The trolley system constitutes a 12-kv network. In the subtransmission class the four-kv network[1] ("high-voltage network") is a typical example of multitapped lines, the transformers which feed the network being tapped from lines such as 13.8 kv. Industrial plants will be found tapped at intermediate points of lines of practically all voltage classes. In many cases these plants have generating facilities in order to provide process steam. Hence, they must be relayed for line faults. Numerous other examples could, of course, be cited.

Classification of Three-Terminal Lines

The simplest three-terminal line is the straight radial arrangement with one

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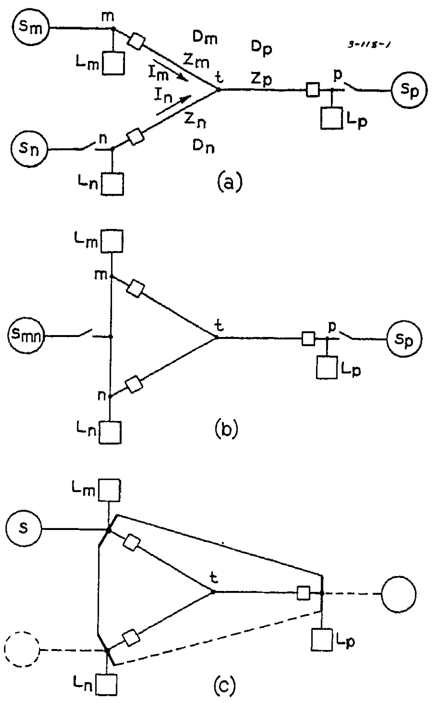


Figure 1. Classification of three-terminal lines

(a). No paralleling ties; one, two or three sources

(b). One paralleling tie; sources on connected terminal, or independent terminal, or both

(c). Paralleling ties between all terminals

S—Power source
L—Load
Z—Impedance

power source and two loads, fed solely over the line in question. As the number of sources is increased or other ties are included between the busses connected by the three-terminal line, additional relaying problems are introduced. Thus, from a relay-protection point of view, three-terminal lines may be classified into seven types as follows:

A. No paralleling ties, Figure 1a.
1. One power source.
2. Two power sources.
3. Three power sources.

B. Paralleling tie between two of the terminals, Figure 1b.
4. Power source on the connected terminals only.
5. Power source on the independent terminal only.
6. Power sources on both.

C. Paralleling ties between all three terminals, Figure 1c (with or without dotted portions).
7. The power source is effective at all terminals.

The classification as regards ground current is, in general, different from that for phase currents. For example, Figure 2 illustrates a three-terminal line with paralleling connections between all terminals for phase currents. However, ground faults on the tapped line cause no ground current in the other two lines. Thus, there are no paralleling connections for ground currents. Also, the two grounded transformers are the only sources of ground current for the tapped line. Thus, the relaying for ground faults is little, if any, different from a two-terminal line. This particular three-terminal line falls in the seventh classification for phase relaying but in the second class for ground relaying. High-voltage bussing throughout would throw it into the seventh classification for ground relaying also.

THE PROBLEMS OF TAPPED LINES

The greater difficulty of relay co-ordination with tapped lines is due to a variety of problems that do not arise with the simpler two-terminal lines. For example, the impedance from one terminal to the two others may be different, introducing a problem in setting distance-type relays. Also, an impedance measurement at one terminal for fault near a second is affected by current entering at the tap point from the third terminal, or by load current flowing to the third terminal. These two problems may occur on any of the seven classifications of three-terminal lines.

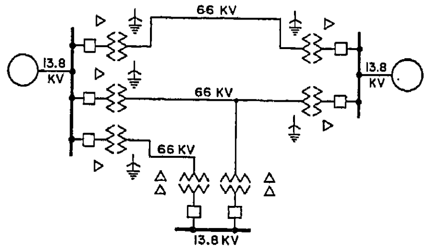


Figure 2. System showing three-terminal line with parallel ties for phase currents and only two sources with no parallel lines for ground currents

In addition, on the latter four classes, which involve paralleling ties, it is possible for fault power to flow out of one terminal for a fault on the line near another terminal. This frequently prevents the use of a straight blocking pilot scheme on lines of this class.

These problems and their solutions are dealt with in the subsequent paragraphs. Quite satisfactory solutions are possible by the use of pilot-wire[3] and carrier-pilot[4] relaying where these are feasible. Frequently the directional overcurrent or distance schemes depend for good operation on the strict maintenance of a normal system setup, or else sacrifice speed to use settings that will co-ordinate under adverse system conditions. The high-voltage network is an exception as will be explained.

Directional Overcurrent Protection

Three-terminal lines in radial circuits can be protected with standard overcurrent time-delay relays, as indicated in Figure 3. When additional sources of power are connected to the circuits, directional elements must be added to the relays. This circuit is indicated by the dotted construction on Figure 3. As the amount of power that must be transmitted over a given circuit is increased, the problem of maintaining system stability during faults becomes more critical and may impose limitations on the amount of time that is allowable for clearing faults. This usually eliminates the use of timed overcurrent relays, except in special cases where instantaneous overcurrent elements will provide the required speed of operation.

The high-voltage network[1] is an ideal example of a multiterminal-line arrangement that can be satisfactorily relayed Each of the many taps is tripped by power reversal to the supply line, the system being so arranged that this can occur only during a fault on the supply line. This requires that all lines feeding a given network emanate from the same bus so that for normal load conditions the supply-line voltages are nearly equal and little or no reverse power flows through any transformer bank.

Many modifications of this basic scheme have been used in which some power reversal can take place when tripping is not desired. If such a reversed flow is due to a fault beyond the next breaker, time is introduced[1] for selection. If it is due to load currents, fault detectors have been successfully used, and, if the reversal can be due to either faults or loads,[2] fault detectors have been used to prevent operation on load currents and timing to provide selectivity for proper fault operation.

Usually such systems are arranged for

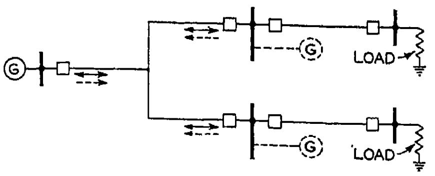


Figure 3. Overcurrent protection of three-terminal line

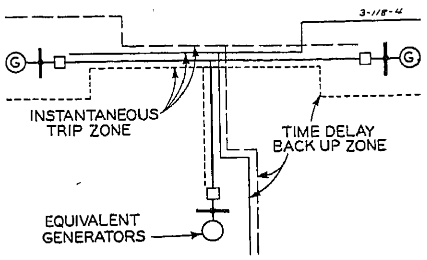


Figure 4. Impedance protection of three-terminal line

only one or two sources of ground current, the taps being tripped by residual voltage.

On long taps with overcurrent protection considerable improvement in selectivity can be secured through the use of angle discrimination. On such lines fault current occurs at a phase angle which could not occur for large load currents or swings.

INDUSTRIAL INTERCONNECTION

When a line is tapped to an industrial plant having generation, it is common practice to segregate essential loads for operation from the plant generator and dump others in event of a line outage. If the same line is tapped for other plants, the problem arises of separating the plant under consideration from the line under conditions hazardous to its operation. One scheme in successful use on many industrial interconnections consists of separation based on any of three indications, provided power flow has reversed and is toward the power company. The three indications are: underfrequency, undervoltage, or generator overload. Any of these occurrences, provided power flow is away from the plant, is taken as sufficient cause for separating and at the same time dumping nonessential loads so that the remaining plant load may be brought within the capacity of the plant generation.

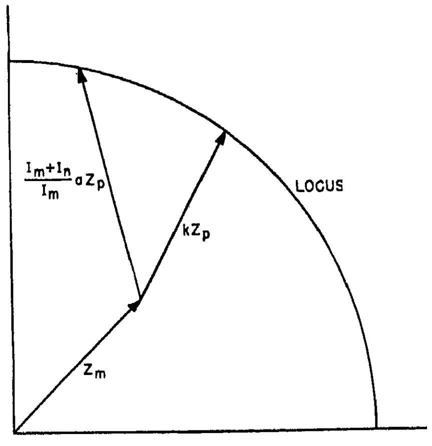


Figure 5. Graphical solution for balance point

$$Z_m+\frac{I_m+I_n}{I_m}\,aZ_p=Z_m+kZ_p$$

Given Z_m, Z_p, I_m, I_n (vectors) and either a or k (scalars) the other can be found by drawing the circular locus of impedance viewed by the relay

Refer to Figure 1a for nomenclature

Relay at m. Normal balance point is at Z_m+kZ_p from m

Balance point with mutual effect is at Z_m+aZ_p from m

The relays normally employed are:

Induction-type overcurrent for generator overload.

Induction-type underfrequency relay.

Induction-type undervoltage relay.

High-speed-type three-phase directional relay.

The overload relay is directional controlled so that it will not start timing unless direction has reversed.

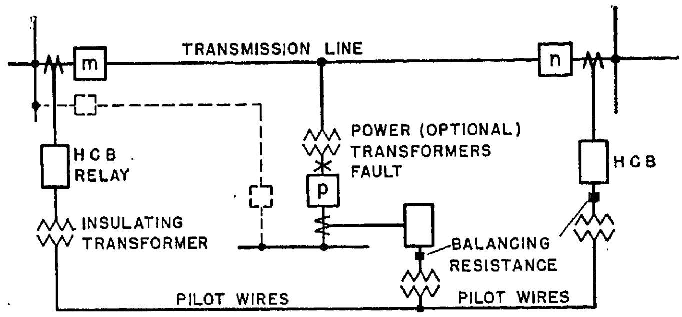


Figure 6. Typical three-terminal power circuit protected by three *HCB* relays

Directional relays are also used, without the voltage, frequency, or current fault detectors, for this purpose.

Impedance or Distance-Type Protection

The general principle of application of distance relays to a three-terminal line is illustrated in Figure 4. As indicated, the instantaneous elements are set to include a zone that has a radius of approximately 90 per cent of the impedance to the nearest station. Faults beyond this zone will be tripped instantaneously by the near relay and, after a time-delay period has expired, at the remote terminals. However, certain problems arise which are not present in the application to two-terminal lines.

UNEQUAL IMPEDANCES TO REMOTE TERMINALS

Even in the simplest case of one power source and no paralleling ties, the three-terminal line presents a problem not present with two-terminal line protection. For example, with a source at *m* only, in Figure 1a, it is desirable to set the first zone element of an impedance relay at *m* for a large part of the line impedance to the next station. If *p* is much further away than *n*, the first zone instantaneous element can be set to trip only to 90 per cent of the distance to *n*. This leaves a considerable section of the line to *p* without instantaneous protection at *m*.

Also, the second zone impedance element at *m* must be set to trip beyond *p* in order to be sure of tripping end zone faults up to *p*. However, with this setting it may reach so far beyond *n* as to encompass another complete line section, whereas for proper co-ordination it should cut off somewhere in the first zone protective region of that next line section.

A similar problem exists with the third zone element which is set where possible to back up completely the subsequent line section.

These problems occur also in the other cases where more sources and paralleling ties are present. It is evident that considerable advantage can be secured from a pilot channel to prevent the high-speed impedance elements from overreaching either of the other terminals, as will be described subsequently.

MUTUAL IMPEDANCE EFFECT

While the impedance relay operates on the ratio of current to voltage it is convenient to hold one quantity fixed and visualize the operation as the other quantity varies. Suppose a constant fault current is considered through the relay at *m* and the fault is gradually moved from the relay toward *p*. At first there is no voltage restraint, and the relay operates. As the fault is moved further away, the voltage restraint on the relay increases directly with the distance or impedance, until a so-called "balance point" is reached beyond which the voltage restraint exceeds the current operating force. Suppose that with a single power source this normal balance point is at 90 per cent of the distance to *p* and takes in 80 per cent of the section from *t* to *p*.

If a second source is introduced at n and the fault is again moved from m toward p, the restraint again increases directly until t is passed. Then, if the source at n provides four times as much current at the first source, the restraint increases five times as fast for distances of fault beyond t, since the current producing the voltage drop is five times as great in this section. The balance point is therefore reached at 1/5th of 80 per cent or 16 per cent of the distance from t to p. Or, in general, if the normal balance point is at an impedance, Z_m+kZ_p, from the relay, with a source at m only, and no paralleling ties, the balance point moves to an impedance

$$Z_m+\frac{I_m}{I_m+I_n}\,kZ_p$$

from the relay when the source or paralleling tie is added.

This "backing-up" of the balance point of a relay caused by current entering at the tap point is termed "mutual-impedance effect." It introduces complications because of the variable location of the balance point depending on the amount of generating capacity connected at each terminal.

However, it may be noted that if the second zone elements are set to include double the impedance of the common branch, tripping will always be assured although it may be sequential. Any current distribution that more than doubles the common branch as viewed from one terminal m, will less than double it as viewed from the other terminal n. The terminal with the larger current will therefore trip, eliminating the mutual effect, whereupon the other terminal will trip.

Mutual Effect of Loads

Even with a single source and no paralleling ties, load currents will have some effect on the balance point of impedance relays, particularly when fault and load currents are of the same order of magnitude. In Figure 1a with a fault at p, current to the load L_n, causes mutual drop in the branch m. Since this load current flows through the relay but does not create proportional restraining voltage by flowing over the line clear to the fault, it causes an overreaching effect which must be considered particularly in the setting of a first zone element. In terms of a normal balance point at a distance, or impedance, Z_m+kZ_p, the addition of the load L_n moves the balance point to an impedance

$$Z_m+\frac{I_m}{I_m+I_n}\,kZ_p$$

from the relay. This expression is identical with that given previously which makes the fraction $I_m/(I_m+I_n)$ greater than one. Starting with a normal balance point at Z_m+80 per cent of Z_p, a load flowing to L_n and constituting 40 per cent of the total relay current I_m, would move the balance point to Z_m+133 per cent of Z_p. (Note: 1/1–0.4 x 0.8= 1.33.) To prevent overtripping, the normal balance point must be reduced. A balance at Z_m+aZ_p with the load present can be secured by making the normal balance point:

$$k=\frac{I_m+I_n}{I_m}\,a \qquad (1)$$

Then, if $a=0.8$, $k=1-0.4/1\times0.8=0.48$. A normal balance point at Z_m+48 per cent of Z_p will be moved to Z_m+80 per cent of Z_p when the 40 per cent load is present.

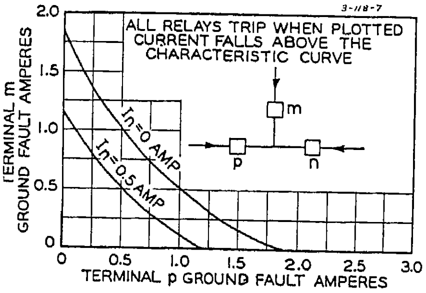


Figure 7. Typical tripping characteristic of *HCB* relays on a three-terminal line for internal ground faults

Relay set for maximum sensitivity. Pilot-wire resistance 200 ohms (each branch)

Negligible capacity between wires

Phase-Angle Effects

In the foregoing discussion the balance-point expression has been treated as algebraic which is, of course, strictly true only if the impedance phase angles are alike and the currents I_m and I_n either in phase or 180° out of phase. More generally the balance point occurs at Z_m+aZ_p where a can be determined by solving the equation

$$\left|Z_m+\frac{I_m+I_n}{I_m}\,aZ_p\right|=|Z_m+kZ_p| \qquad (2)$$

A graphical solution is shown in Figure 5.

Pilot-Wire and Carrier Pilot Protection

Pilot protection using either wire or carrier channels has proved to be quite an effective solution to the problems of relaying three-terminal lines. Two common types of pilot protection will be discussed here. They are known respectively as, first, the directional comparison or blocking type which is used with d-c pilot-wire or carrier-current channels, and second, the two-wire a-c pilot-wire type.

Directional Comparison or Blocking Type

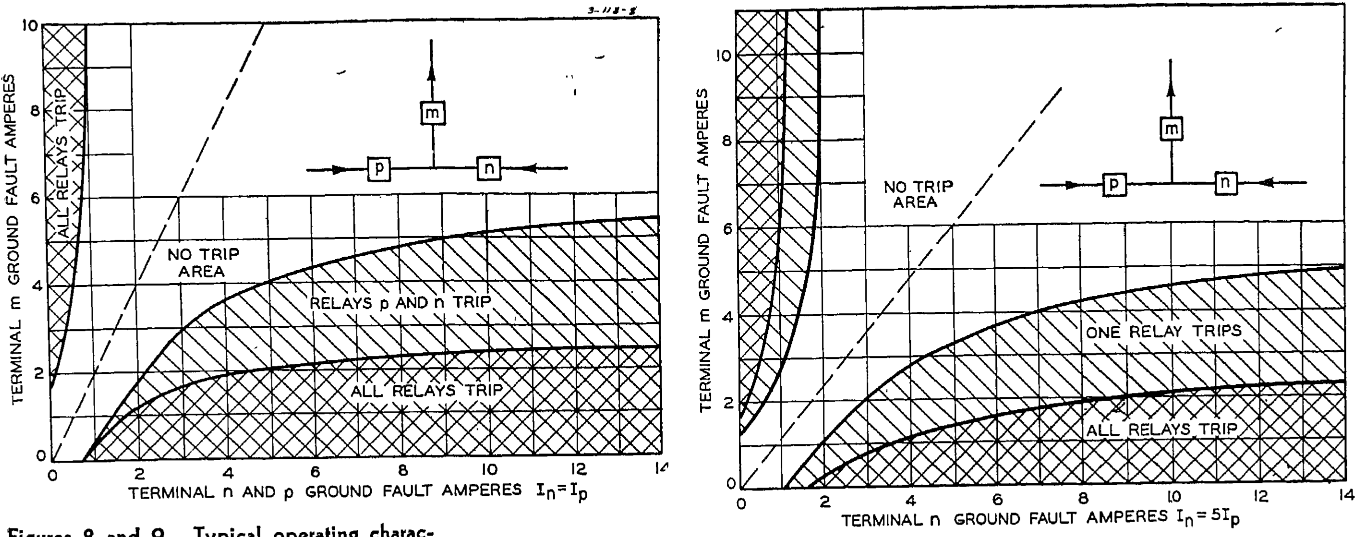
In the blocking scheme, directional fault-detector relays at each terminal trip for any faults in the tripping direction unless blocked by a signal from the far end indicating the fault to be beyond the far end of the line. This signal is usually initiated by a directional element which operates when power of fault magnitude flows out of the line at that terminal. Fault power flowing out at one terminal of a three-terminal line, to an external fault, will correctly block tripping at the other two terminals.

Such a blocking scheme is used with either wire or carrier pilot. One arrangement utilizes the step-type directional impedance relay as the fault-detecting element, and as the directional element for controlling the blocking signal, corresponding ground-current elements being used. The backup characteristics of step-distance relaying are obtained in this scheme without added impedance elements.

As previously explained, on systems with paralleling ties, as in Figure 1b or c, fault power may flow out at one terminal for a fault on the protected line near another terminal. However, with the step-distance arrangement the first zone element is used independently of carrier or pilot blocking and results in immediate tripping of the terminal nearest the fault. Thereafter fault power no longer flows out, and the carrier- or pilot-controlled relays promptly trip at the other two terminals.

Also, in extreme cases the mutual impedance effect, previously described, may prevent a tripping element from reaching to a fault at the far terminal. This may result in the blocking of pilot tripping at all terminals. However, operation of the first zone instantaneous element will in many cases open the breaker nearest the fault and remove the mutual effect. Pilot-controlled relays can then trip normally at the other terminals.

To summarize, distance-type pilot-wire or carrier protective systems can be applied to any type of three-terminal power circuit. Their operation may be slightly delayed when power flows out of one terminal of the faulted line or when mutual impedance effect prevents a



Figures 8 and 9. Typical operating characteristics of *HCB* relays on a three-terminal line

Instantaneous direction of ground-fault currents indicated by arrows, all currents in phase

Relay set for maximum sensitivity (0.5 ampere ground current per terminal)

Pilot-wire resistance 200 ohms (each branch)

Negligible capacitance between wires

tripping fault detector from reaching a fault near a far terminal.

Two Wire A-C Pilot-Wire Type

The *HCB* single-element a-c pilot-wire scheme,[3] developed in 1937, which has been widely used on short two-terminal lines, is also applicable to short three-terminal lines. In fact, such applications have been in successful operation in United States and Canada since 1939. The success with these lines and the greatly increased number of such applications at the present time has stimulated a considerable study of the limits of application of this type of equipment to determine how far it can be extended. Operating characteristics have been determined as the current distribution is varied, or the length of the tapped lines increased with corresponding increase in pilot-wire resistance and capacity. Some of the significant results of this analysis are presented here.

Principle of Operation

The *HCB* scheme of pilot-wire protection operates essentially as an extended bus-protection scheme, tripping all terminals whenever the totalized current to a fault within the protected zone exceeds a predetermined setting. It has variable ratio characteristics, produced by the restraining coils and a saturation device, that makes it relatively insensitive to ratio errors of current transformers on heavy through faults. The ratio characteristics are the same in percentage for any type of fault, phase, or ground; however, the relay has separate adjustments for phase and ground currents, so that the actual magnitude of ground current required to trip can be, and usually is, set considerably lower than the phase-current trip.

It is evident from the foregoing discussion that this scheme differs from the blocking types in that flow out at one terminal will not necessarily block tripping if the total net flow is "in" to a fault on the line. It is, therefore, ideal in principle, producing immediate tripping of all terminals for internal faults.

As the number of terminals is increased above two, and as the length of the pilot wire is increased, departures from the ideal of "extended bus protection" already described begin to be observed. For example, the minimum trip value increases with the number of terminals, and some variation is noted between the fault current required to trip different terminals, depending on the current distribution and the pilot-wire resistance and capacitance.

In the *HCB* pilot-wire scheme, restraint current is circulated through the pilot wires for through-fault or normal load conditions. With three terminals the three pilot wires are all brought to a common point and paralleled, their resistances being equalized up to this common point by means of balancing resistances as shown in Figure 6. For a through fault or load current flowing in at two terminals and out the third, restraining pilot-wire currents flow from the two terminals to the pilot-wire junction where they combine to form the correct pilot current to the third terminal. For faults within the protected zone the relay voltages act in opposition over the pilot wire, reducing the restraining currents and building up voltages which pass large currents through the shunt operating coils.

The operating characteristics of the *HCB* pilot-wire-relay scheme are illustrated by the charts of Figures 7, 8, and 9. If each relay is given a ground-current setting of 0.5 ampere, all relays trip whenever the total current to a fault in the protected zone totals approximately 1.5 amperes, or 0.5 ampere per terminal. The minor variations from this approximation are shown for moderate pilot-wire lengths in Figure 7. With equal distribution 0.5 ampere per terminal trips, whereas 1.9 amperes in terminal *p* alone is required to trip all three relays, the difference being due to the greater restraint current under the latter condition. This difference is usually of small significance.

The characteristic curves are given only for ground current, but it should be understood that the same curves apply for phase currents if the current scales are multiplied by a constant, that is, by the ratio of phase setting to ground setting given the particular relays.

The curves of Figures 8 and 9 are used first of all to show the amount of current-transformer inaccuracy that would be required to cause misoperation on a through fault. All current entering *p* and *n* must leave at *m* for a through fault. The currents *p* and *n* are considered equal and plotted as abscissa in Figure 8, while the current out at *n* is plotted as the ordinate. The curves separate trip from nontrip areas. Thus, two amperes in each of *n* and *p* (abscissa 2), and four amperes out at *m*, falls on the dashed line well within the no-trip area. In fact even should the ratio error of the current transformers at *m* be so great that only two amperes was delivered, the relay would still not quite trip. However, dropping to 1.5 amperes would cause undesired tripping at p and n. The burden is moderate (1.6 ohms in residual circuit) so that current-transformer errors beyond those permissible according to Figure 8 would not be expected.

It was mentioned that the relay tripped on the basis of net current flow into the protected zone and hence operated even with some flow out at one terminal. There is obviously a limit to how far this can be carried, since the greater the departure from equal feed in at all three terminals the greater the restraint current flowing and the higher the operating value. With sufficiently high current out at one terminal, tripping will be prevented. Figure 8 is also intended for determining whether correct tripping will be obtained during an internal fault with maximum flow out at one terminal. It shows for example that if two amperes (secondary) flow in at each of p and n terminals, as much as 1.0 ampere leaving at m will not block tripping.

Curves similar to Figure 8 have been prepared for other than equal values of currents at p and n. Figure 9 shows the corresponding characteristic with the current at n equal to five times that at p.

It has been found from experience with a number of applications that the flow out is not sufficient to block desired tripping, as determined from data such as Figures 8 and 9. It can be concluded, therefore, that in practical cases, the *HCB* relay as used on three-terminal lines provides instantaneous tripping for all internal faults on three-terminal lines. Also it provides adequate margin to prevent operation on through faults even with quite large current-transformer inaccuracies. As mentioned, a number of installations have proved this to be a reliable means of obtaining high-speed (one cycle) relay protection on three-terminal lines. Standard equipment is used so that existing circuits frequently can be expanded simply by adding a third relay.

The pilot wires used should preferably be enclosed in cable sheaths, protected from lightning, induction, and differences in station ground potentials so as to maintain a continuous circuit at all times.[5,6] On three-terminal lines the shunt capacity between pilot wires should not exceed 0.5 microfarad per terminal for 500-ohm circuits. Continuous d-c supervision can be supplied with the relay to ring an alarm whenever any branch of the circuit is opened or faulted.

Conclusions

The co-ordination of directional over-current and distance-type relays is more difficult in three-terminal lines because of two principal problems.

1. Unequal impedances to remote terminals.

2. Mutual impedance effects from fault and load currents entering or leaving at the tap.

Blocking-type pilot schemes assist in solving these problems, but on three-terminal lines with paralleling ties undesired blocking may be produced by:

3. Power flow out at one terminal to a fault within the protected zone near another terminal.

The first zone element of step-distance-type pilot schemes solves this problem by operation independent of the carrier-current or pilot-wire blocking, but at the expense of a slight time delay.

The *HCB* two-wire a-c pilot-wire scheme circumvents all of these problems and provides uniform high-speed protection even in the more difficult cases of three-terminal line protection. Typical operating characteristics for three-terminal line operation are presented in the paper.

References

1. PRIMARY NETWORK SYSTEM, H. Richter. *Electric Journal*, December 1931.

2. PENNSYLVANIA RAILROAD NEW YORK–WASHINGTON–HARRISBURG ELECTRIFICATION—RELAY PROTECTION OF POWER-SUPPLY SYSTEM, E. L. Harder. AIEE TRANSACTIONS, volume 58, 1939, June section, pages 266–73.

3. A SINGLE-ELEMENT DIFFERENTIAL PILOT-WIRE RELAY SYSTEM, E. L. Harder, M. A. Bostwick. *Electric Journal*, November 1938.

4. A NEW HIGH-SPEED DISTANCE-TYPE CARRIER-PILOT RELAY SYSTEM, E. L. Harder, B. E. Lenehan, S. L. Goldsborough. AIEE TRANSACTIONS, volume 57, 1938, January section, pages 5–10.

5. PROTECTION OF PILOT-WIRE CIRCUITS, E. L. Harder, M. A. Bostwick. AIEE TRANSACTIONS volume 61, 1942, September section, pages 645–52.

6. PILOT-WIRE CIRCUITS FOR PROTECTIVE RELAYING—EXPERIENCE AND PRACTICE, AIEE relay subcommittee. AIEE TRANSACTIONS, volume 62, 1943, May section, pages 210–14.

Arc-Furnace Control by Regulex Exciters

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FOR a number of years prior to the present urgency for the production of high-quality steel, the development in control methods for electric-arc furnaces remained substantially stationary in this country. These devices were in the general form of vibrating regulators or contact-making ammeters which rapidly connected the electrode motors to and disconnected them from a constant d-c voltage source. In Europe, meanwhile, various methods employing Ward-Leonard or hydraulic control means were developed, some of which were reported to result in smoother operation under the highly fluctuating transient conditions of the arc.

Some six months prior to Pearl Harbor, study indicated that with stepless regulating or control means available in this country improved results might be effected. Such means consists of a rotating regulator in the form of a Regulex exciter. The first of this type control, here discussed, has now been in operation long enough for a definite judgment of operating results.

Qualities Required in a Regulator

1. Actuation from a measure of arc or heating energy.

2. Correction increasing in value as deviation from normal increases and decreasing to zero as deviation decreases to zero.

3. Quick response.

4. Stable antihunt means to prevent overshooting.

These requirements, of course, need to be met in any successful closed control system for governing or regulating. However, in the arc furnace the regulating problem is unusual because of the high degree of instability of the electric current and power in the arc.

By referring to A and A^1, Figure 7, film 2, it will be seen that fluctuations occur about every six cycles[1] near the beginning of a melt in a steel furnace. This is due in part to rapidity of changes in ionization, varying degree of rectification, and so forth. Obviously, it would be impractical to move an electrode system weighing perhaps a ton, in a six-ton furnace, fast enough to correct for these variations by control of arc length. Cur-

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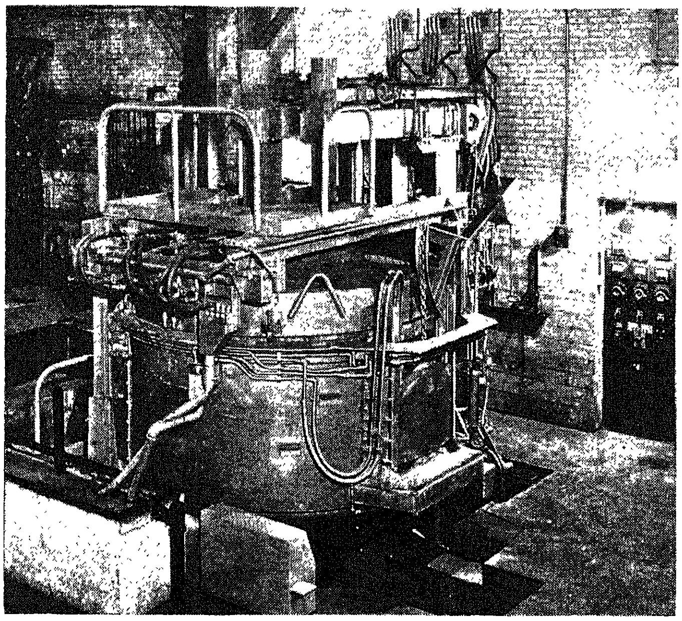


Figure 1. Six-ton top-charge arc furnace with operator's panel at right

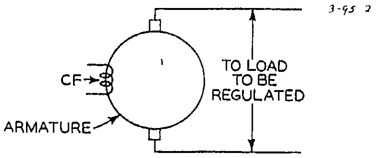
rent is limited in such transients to a safe maximum value by transformer and lead reactance and where necessary an external reactance, as $X2$ in the equivalent circuit of Figure 3. But, under the same conditions, the variations in average values, from maximum to minimum as from A to B, Figure 7, film 2, occurs every one to two seconds. This is due in part to metal melting away, electrode melting, and so forth, which changes the arc length.

Thus a good regulating system must hold the average value to minimum variation. The Regulex exciter was selected for these requirements.

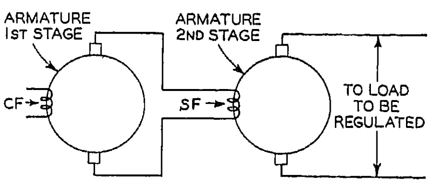
Rotating Regulators

Amplification

In a d-c generator rotating at constant speed, such as Figure 2A, the output power is controlled by the excitation winding *CF*. The machine is recognized as a power amplifier, and the control power in winding *CF* is a small percentage of the power output. This percentage is two per cent to five per cent in a machine of standard commercial design giving an amplification factor of 50 to 20, respectively. This ratio is dependent on frame size and design, and in general the larger the frame size the greater the amplification factor for a given output voltage because the larger the frame the more flux per watt excitation is set up. Furthermore, by connecting two such machines in tandem as in Figure 2*B* the ratio of power output to control power is the product of the amplification factors of the individual machines. Therefore, with a given control power available, in the form of a measure of the quantity to be controlled, a wide variety of amplification factors are available to the engineer to obtain the necessary power output. The power output then is used to react on the quantity to be controlled whenever it varies from normal.



A— SINGLE STAGE



B— TWO STAGE

Figure 2. Regulex exciters

For example, in Figure 3, the control power available is current-transformer five-ampere metering and arc potential. Field 4 is designed for current-transformer metering current, and field 5 is designed for corresponding volt-ampere and ampere-turn capacity. The Regulex exciter 3 is the first stage of the amplifier and is of sufficient size to excite the electrode generator 2, which is the second stage. The second stage then is made of suitable capacity to drive electrode motor 1. This motor acts to change arc length when arc current and voltage vary from normal. Obviously for small electrode motors only one generator need be used.

Stability

The next most important consideration is stability of the system to prevent overtravel and "hunting." In the Regulex arc-furnace control, the actuating medium in the system is the resultant excitation of two opposed fields, 4 and 5, Figure 3, in the Regulex exciter. The control power is the sum of the excitation watts of these two fields. These fields are equal in ampere turns at normal value of arc watts or heating energy (the aforementioned quality 1 required in a regulator). No actuating flux being present, under this condition, no voltage is applied to the electrode motor.

Curve *B*, Figure 4, gives a typical volt-ampere arc characteristic. The change in voltage is not linear with change in current but is sufficiently large to satisfy quality 2. For small deviations from normal the actuating flux is essentially proportional to the deviation of arc watts from normal and the resultant power output to produce correction is essentially proportional to this deviation. This produces an inherently stable system where mechanical and electrical inertias can be neglected, thus signifying quality 4. Where these factors are appreciable, an anticipation of "cutoff" of the actuating flux proportional to such inertias is required. The amplification together with design for low inductance in all circuits satisfies quality 3 as will be later shown.

Sensitivity

The sensitivity of the system depends on amplification ratio, friction in the electrode system, and hysteresis in the electrical machines. As will be seen from the volt-ampere characteristic, curve *B*, Figure 4, at very low currents the change in arc volts is greater per unit change in current than at higher current values. For this reason good sensitivity is maintained inherently down to very low currents. The system is quite stable at 10 to 15 per cent current which is necessary to prepare furnaces for operation after relining (burning bottom).

Arc-Furnace Control

An arc-furnace installation is subject to greater fluctuation in power and surges[2] than other installations requiring control because of:

(*a*). More frequent switching.

(*b*). Wide range in primary transformer taps to provide necessary secondary voltage.

(*c*). Large reactance to limit maximum current.

(*d*). Varying amount of d-c component in the arc.

(*e*). Rapidly fluctuating state of ionization in the arc.

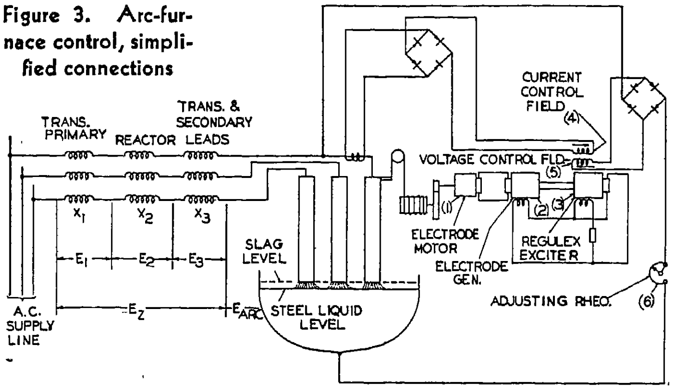


Figure 3. Arc-furnace control, simplified connections

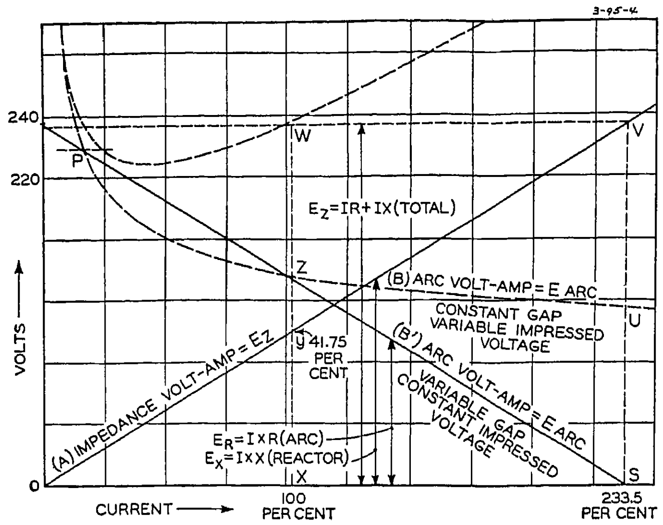


Figure 4 (right). Typical arc-circuit characteristics

The last item *e* has greatest effect from the control standpoint.

The complete system, here under consideration, consists for each electrode, of a Regulex exciter controlling a generator of suitable size with its armature connected electrically directly to the armature of an electrode motor which raises and lowers the electrode. In addition, there are the necessary control switches, meters, indicating lights, and so forth. This equipment is duplicated for each electrode. For a three-phase furnace three Regulex exciters, three genrators, and an a-c driving motor form a small seven-machine motor generator set. The complete connections are shown in Figure 5. The motor generator set, as shown by the connections, may be supplied from the furnace bus, thus making the entire installation self-contained, and not dependent on a separate d-c power source. Figure 6 shows the control equipment from the rear of the operator's panel. The panel at the left mounts, plate-type rectifiers, control resistors, and so forth. On small electrode motors, generally under one horsepower, the Regulex exciter armatures are connected directly to the electrode motors, thus resulting in a small four-machine control unit.

As shown in Figure 5, current settings in each phase are made by rheostats *R*1, *R*2, and *R*3 in the arc-potential circuit. By changing, for example, the excitation of *VF-A*, the phase current is automatically changed, since the differential flux between *VF-A* and opposed current field *CF-A* produces voltage to run the electrode in the proper direction (raise) until the fields are equalized.

Four voltage positions normally are provided on the transformer tap changer. On the highest voltage all of resistance *R1A*, *R2A*, and *R3A* in the potential circuit are connected in the circuit. When the circuit is changed to the next lower

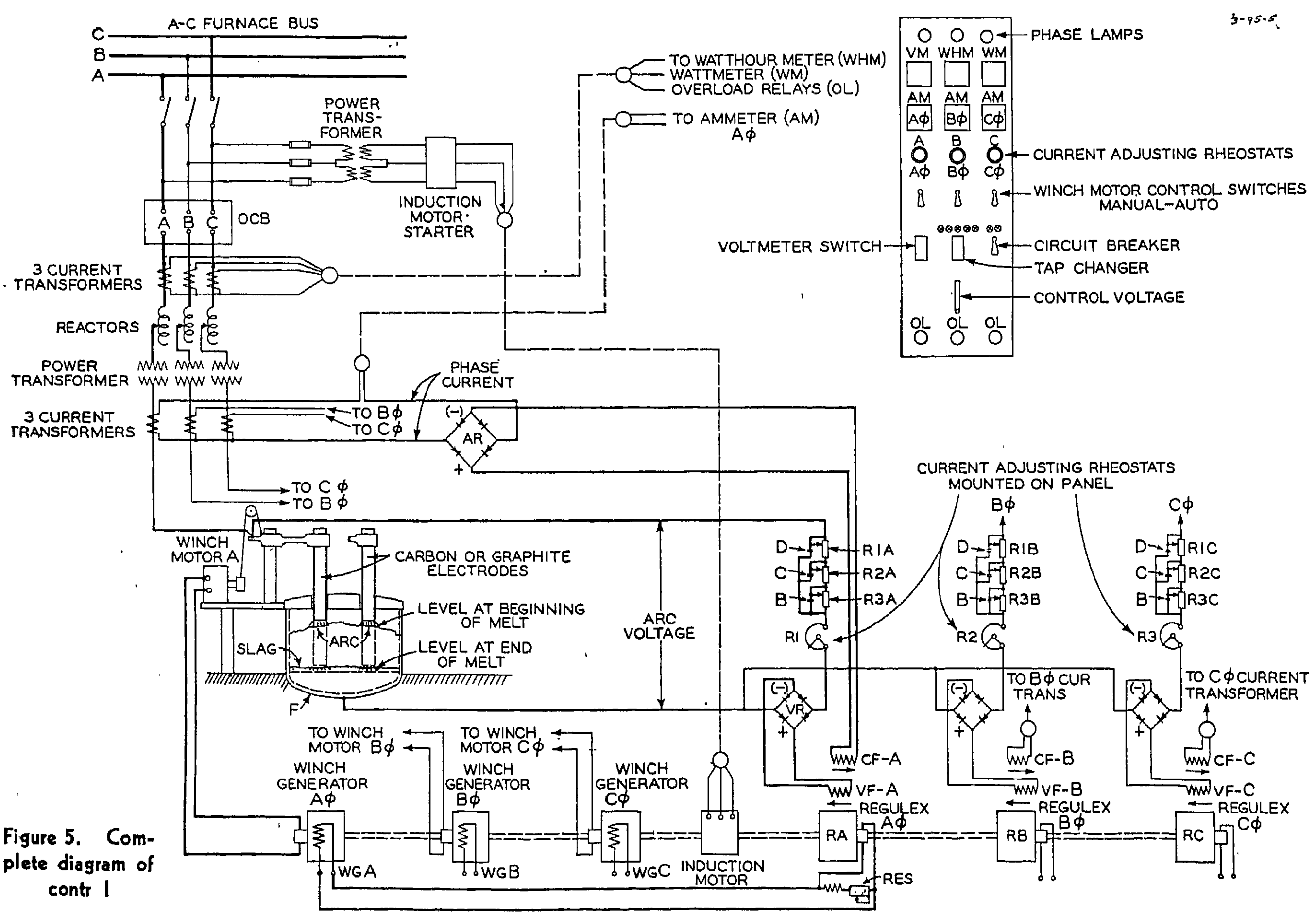


Figure 5. Complete diagram of control

voltage, contact *B* automatically short-circuits resistor *R3A* so that on the new voltage the current setting is not changed. Contacts of *B* also perform the same function on the other phases, and for other voltages contacts *C* and *D* act in like manner.

Operation of the Control in Service

A good image of the net operation of the control can be obtained from examination of oscillograms taken with a furnace in operation. The films of Figures 7 and 8 were taken on a steel furnace of six tons nominal charge capacity. Arc energy is supplied through a furnace transformer from a 22,000-volt three-phase 60-cycle line. The analysis of the melt was

Carbon	0.35
Manganese	0.14
Silicon	0.43
Phosphorus	0.035
Sulphur	0.037

In all films, which were taken on a single phase, the oscillogram traces starting at the top of the film and progressing downward measured the following:

1. Arc volts.
2. Electrode (or winch) motor, armature volts.
3. Arc amperes.
4. Voltage-control-field amperes (rectified).
5. Current-control-field amperes (rectified).
6. Electrode (or winch) motor armature amperes.

All values are rms except rectified Regulex-control-field currents which are peak values.

In film 2, Figure 7, taken 15 minutes after the start of the melt, just prior to sudden current increase, at *A*, the electrode motor was operating at 75 volts and lowering the electrode at 10.85 inches per minute. With the sudden current increase the motor was reduced to zero speed in 26 cycles. However, it was not decelerated at a constant rate, as can be seen from the motor armature current. As the average difference in current and volts decreased, the rate of deceleration decreased. The motor reached practically zero speed 26 cycles later and remained so for approximately 35 cycles up to point *C*. At this point the average arc energy had dropped below normal, and the motor accelerated for 35 cycles until it had 90 volts across its armature. At this point the average arc energy became essentially normal, and the electrode speed remained at 14.35 inches per minute lowering, and the motor armature current ran to approximately zero.

As will be shown later, between points *E* and *B*, mutual inductance between the two opposed control fields prevents response to the peak values of only a cycle or so duration. However, following point *B*, the current rose sharply from about 5,000 amperes to 18,400 amperes, and the motor was decelerated to zero speed in about 10 to 11 cycles and reversed. Here a high "pump-back" current is regenerated in the electrode-motor armature.

Consider the action at point *F*. Prior to reaching this point because of too low arc energy, the electrode motor had been accelerated to lower the electrode until 106 volts were across its armature. The increase in current at this point, with little decrease in voltage starts decelerating the motor. However, as previously mentioned, at this point in the heat, metal is running away from the electrode as it melts, thus tending to increase arc length inherently and to reduce current. At the same time the electrode is lowering. Consequently, the rate of lowering the electrode decreases only until the average *actuating measure* of arc watts is reduced to normal which is not necessarily *zero speed of the electrode.* In examining the electrode-motor armature volts for the entire length of film 2, the electrode is raised four times, with only slight movement each time. The total area under all of these raising voltage swings below zero, compared to the total area under the lowering loops above zero, shows that the electrode is lowered through the scrap at such a rate as to maintain as near constant average heating value as the response of the system to the extreme fluctuations will allow.

Film 4, the lower one in Figure 7, was taken 65 minutes after the start of the melt of film 2. Much of the steel has been melted down, and conditions are steadier. The film speed is such as to show the general wave shape of current and voltage. At point *H* on the film, it will be noted that current jumped from 5,240 amperes to 13,100 amperes on successive peaks. However, the rectified-voltage control-current peak increased on this half cycle, because of mutual inductance, even though the voltage was sharply reduced. But the average of the next four half cycles is reduced from the previous four half cycles with a net small response from the Regulex unit. This characteristic is of particular value in refining at the end of the melt to prevent false movement when the metal is "boiling." When a bubble rises against the electrode and disappears within a cycle or two, after bursting, no movement of electrode results.

Film 8 at top of Figure 8, was taken one hour and 55 minutes after the start of the heat of films 2 and 4 and consequently during the refining period. Iron ore and

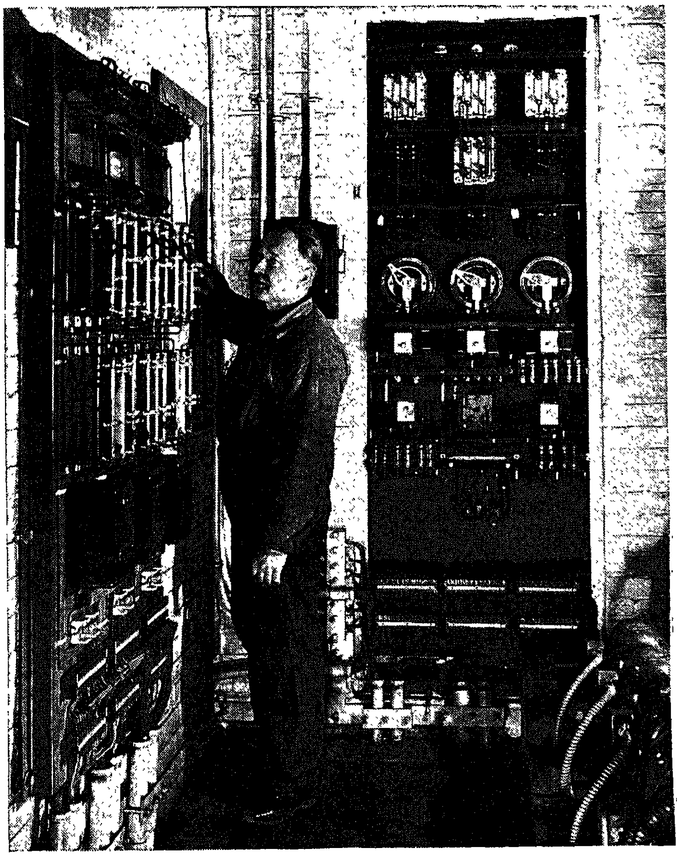


Figure 6. Rear view of operator's panel showing auxiliary panel and Regulex exciter motor generator set

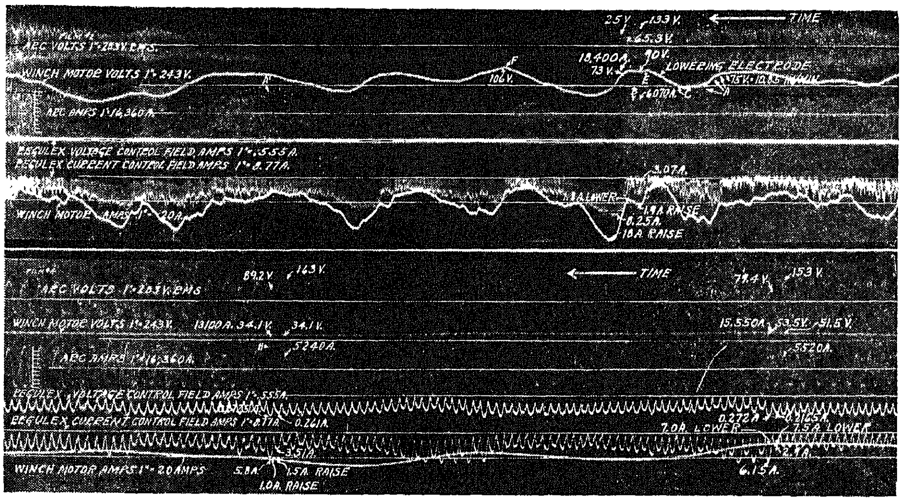


Figure 7 (above). Film 2 and film 4

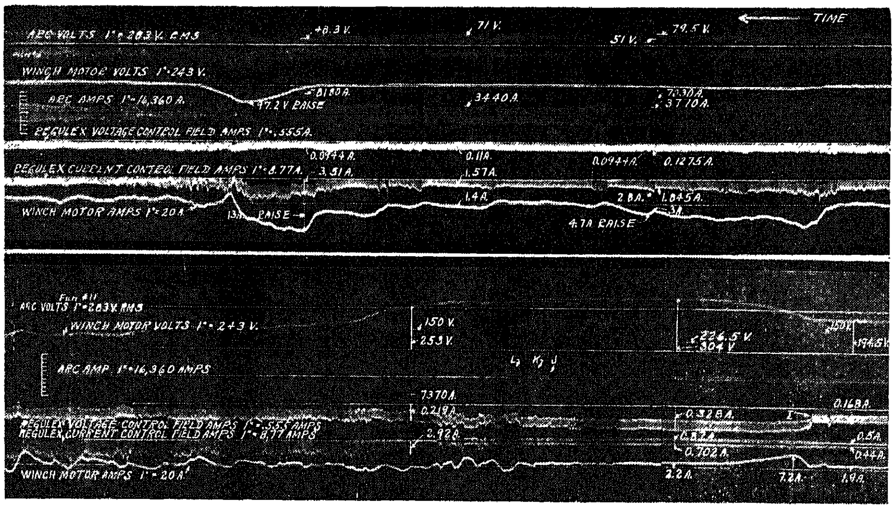
Film 2 taken 15 minutes after start of melt. Transformer on *A* tap, 205 volts phase to phase, 14.3 per cent impedance. Time 10:15 p.m. Film speed approximately $1^1/_4$ inches per second. Heat 21071. Saturday, November 21, 1942. Charge 2,000 pounds turnings, 9,600 pounds gates, 6,400 pounds plates (nine tons total)

Film 4, same heat, taken 65 minutes after start of melt. Time 11:05 p.m. Transformer on *A* tap, film speed approximately 23 inches per minute

Figure 8 (below). Film 8 and film 11

Film 8, same heat taken one hour 55 minutes after start of melt, 11:55 p.m. Transformer tap *C*, 127 volts phase to phase, 9.4 per cent reactance. Film speed approximately $1^1/_4$ inches per minute

Film 11, heat 21075. Sunday, November 22, time 3:10 p.m. (furnace shut down approximately 7:00 a.m. Sunday morning, restarted 3:10 p.m.). Start of melt. Transformer tap *A*, 205 volts phase to phase, 14.3 per cent reactance. Film speed approximately $1^1/_4$ inches per second. Same scrap and analysis as on film 2



other elements had been added shortly before and created violent boiling sufficient to necessitate a cutoff of power to prevent boiling over. The film shows regulation just after reapplication of power after boiling had subsided somewhat.

Film 11 was taken at the beginning of a heat next day. The control was purposely set so that when it was placed under automatic regulation to lower electrodes onto loose scrap, one electrode would strike the scrap in advance of the one being recorded. At point *I* to the right of the film when the electrode touched the scrap placing full-phase voltage between electrodes, the electrode motor speed was increased correspondingly. At points, *J*, *K*, *L* when the electrode first came in contact with the loose scrap, the arc was "struck" and broken some eight times before being firmly established. Thereafter, it was not lost for the duration of the heat except for interruptions by the operator for tap changing, and so forth.

It is to be noted particularly how electrode motor speed was reduced as the arc was established, until at the extreme left of the film it has reached a speed comparable to the lowering speeds of film 2. Also of interest is the low magnitude of arc current when the arc is initially established.

Power Consumption

Power consumption is, of course, dependent on many variables including type of material melted, method of feeding material into the furnace, use made of the furnace such as melting or refining, and so forth, so that kilowatt hours per ton vary from one installation to another. The control is only one element in any comparison. However, on furnaces of the same type and rating, in the same plant, evidence of more uniform resultant heating has been reflected in more heats per day as compared to older methods of control. Figures are not given because the results are unexpectedly favorable and need verification in other installations to be put into service, where comparisons with previous equipment can be made.

Effects on Power System

Previous papers[3] before the Institute have shown that maximum disturbance on the power system occurs when the arc is made or broken. From film 11, Figure 8, and the other films, it may be seen that once the arc has been established firmly, at the beginning of the melt, it is not broken again before the end of the melt, except for interruptions for voltage change, and so forth.

Also, it has been found that a lesser reactance may be used than with older methods, thus improving power factor to some extent with less net voltage disturbance. Comparative data are not available on this general comment.

Conclusions

1. A stepless direct acting regulator is readily adaptable to highly fluctuating electrical quantities.
2. Application of such control to arc furnace has improved efficiency and reduced power consumption and maintenance.

References

1. ARC-FURNACE LOADS ON LONG TRANSMISSION LINES, T. G. LeClair. AIEE TRANSACTIONS, volume 59, 1940, April section, pages 234–9.

2. A STUDY OF VOLTAGE TRANSIENTS IN ARC-FURNACE CIRCUITS, J. B. Hodtum, J. B. Rice. AIEE TRANSACTIONS, volume 62, 1943, August section, pages 556–62.

3. TRANSMISSION-LINE AND SYSTEM PROBLEMS IN SUPPLYING LARGE ELECTRIC-ARC FURNACES DURING WARTIME, B. M. Jones. AIEE TRANSACTIONS, volume 62, 1943, April section, pages 197–202.

Aircraft Electric Power-Supply System

J. E. YARMACK
MEMBER AIEE

Synopsis: An ever increasing demand for electric power in aircraft has been felt for some time. The necessity of using an electric storage battery started the practice of accompanying it with a d-c generator, first of low current and voltage capacity, later of 50-ampere 12-volt rating, and for the past few years of 200-ampere 24-volt output. In the largest four-engine aircraft, even four generators of maximum capacity do not furnish ample electric energy. Experiments have proved so far that generation of a-c power and application of lightweight transformers, together with its conversion to direct current by means of selenium rectifiers, is entirely a reliable and practical way of attaining large, trouble-free electric plant capacity in aircraft. The subsequent pages and illustrations portray the current progress made with lightweight power transformers and rectifiers in the a-c-d-c aircraft systems. They also represent at least a partial answer to the questions raised by Lieutenant Colonel Holliday[1] when he discussed problems of applications of electric power in aircraft two years ago.

THE development of a-c–d-c power systems for aircraft application started with a view of achieving two main objectives:

1. Augmenting the source of electric energy offered by conventional d-c generators.
2. Alleviating the difficulties of commutating machinery and voltage limitation under conditions of high-altitude operations.

Furthermore, having alternating current as a primary source of electric energy, some of the a-c powered equipment in the aircraft can readily be used with or without resorting to transformers, whereas d-c operated devices may be powered through rectifiers with or without transformers.

The first stage of early development was to duplicate the largest d-c generator of 200-ampere rating, operating the so-called 24-volt system, with the output voltage set at 28.5 volts in order to maintain the proper charging rate of the battery.[2] The weight, efficiency, and, to some extent, the bulk of the generator were to be at least duplicated or, if at all possible, were to be met more favorably with a combination of alternator and rectifier.

The paramount requirements of the aircraft power rectifier narrowed down to the following three:

1. High efficiency.
2. Minimum weight.
3. Satisfactory service.

An efficiency of 84 per cent appeared to be feasible and acceptable. A unit of six-kw rating, with suitable enclosure and connecting terminals, would receive favorable consideration if its weight could be in the neighborhood of 15 pounds. The larger-sized units are to be a multiple of this one. Satisfactory service could only be expected if the performance of the rectifier is:

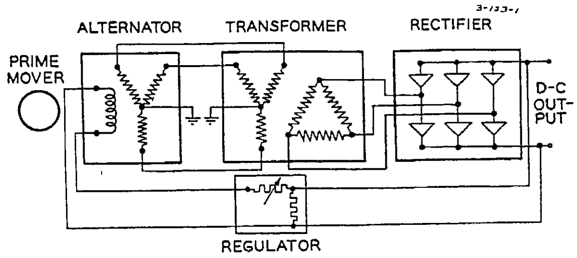
1. Fairly uniform under extreme temperature, humidity, and altitude conditions.
2. Unaffected by vibration.
3. Capable of maintaining good operating characteristics for at least 500 hours if not longer and with a reasonably small amount of air-blast cooling.

The transformer, on the other hand, from the very start presented one important problem, that is, the attainment of the lightest possible weight. The air-blast cooling was set out to be a definite means of meeting this requirement. As it happened, however, the latest kind of material and the unique design produced very gratifying results.

The requirement, design, and performance factors of the alternator constitute an ample subject for their own discussion. It is sufficient to state here that even the early alternator, powering a 200-ampere 30-volt rectifier, was 26 pounds in weight and 90 per cent efficient. Several selenium rectifiers were designed, built, and tested under low and high temperatures, rarefied air, and excessive vibration conditions. Their performance was in every respect satisfactory and, therefore, promising.

In order to evaluate fully the soundness of higher power generations,[3] plans were drawn to build an aircraft electric system capable of delivering a d-c output of 800 amperes, 30 volts, under varying temperature and humidity conditions and with a reasonably small amount of air-blast cooling. Such a system, Figure 1, comprising alternator, transformer, and rectifier, was built and tested.

Figure 1. Block diagram of aircraft electric plant of 800-ampere 30-volt d-c rating



Rectifier

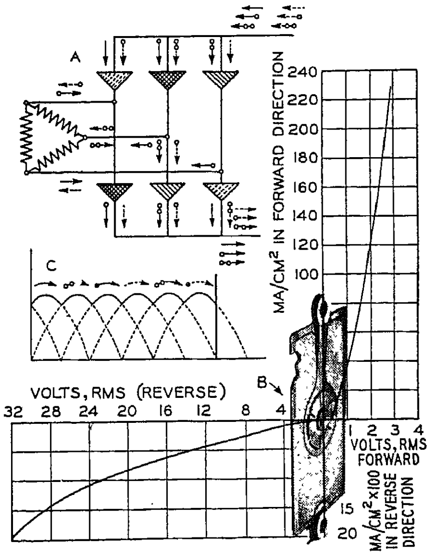
Even during early experiments with a 200-ampere rectifier, it became evident

Figure 2 (right). High-voltage selenium rectifier plate

A. Diagram of three-phase bridge circuit and distribution of current in its six arms

B. Operating characteristic, forward and reverse voltages in rms values, and the currents in arithmetical values, as they function in three-phase bridge circuit. Cut shows four layers of rectifying element: back electrode, selenium layer, barrier layer, and front electrode

C. The net result of performance of the six arms of the bridge circuit during each cycle



Paper 43-123, recommended by the AIEE committee on air transportation for presentation at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943. Manuscript submitted April 23, 1943; made available for printing May 26, 1943.

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The author wishes to express thanks to the officers of the United States Army Air Forces, engineers of the Chrysler Corporation, and to the colleagues of his own organization for their co-operation in carrying on these experiments.

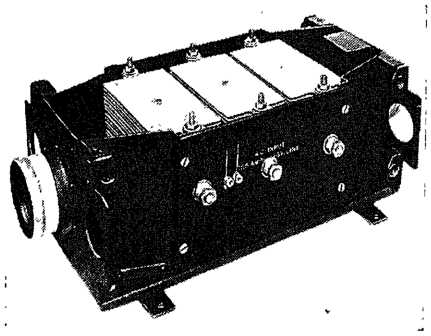


Figure 3. Aircraft rectifier, three-phase input, 200-ampere 30-volt d-c capacity

Top and side covers removed. Weight 15 pounds. Rectifying elements occupy less than 0.25 cubic foot and weigh 11 pounds

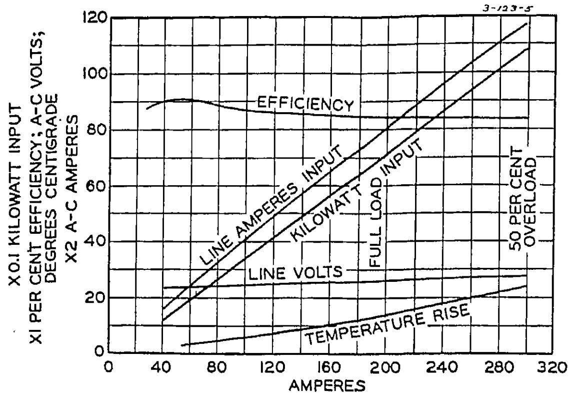


Figure 5. Operating characteristics of rectifier unit shown in Figures 3 and 4

Rated at 200 amperes, 30 volts, at ambient temperatures from minus 70 to plus 50 degrees centigrade. Unit is capable of withstanding 50 per cent current overload continuously for ten hours

that if selenium rectifiers were to play a role in 24-volt aircraft systems, the design and technique of the manufacture of selenium plates had to be modified at least to the extent of using a single selenium plate in series. Such a plate had to be of at least 28-volt rms rating and still of the lightest possible weight.

This plate, as finally developed, weighs $1^1/_4$ ounces bare and $1^3/_4$ ounces when equipped with the necessary hardware including fuse, Figure 2. In the three-phase bridge circuit, selected as most suitable for the aircraft power conversion, this selenium plate accounts for 12.5 amperes of the entire current output at 30 volts d-c with 350 cubic feet of air per minute. It is fitting here to once more reiterate the function of this type of rectifying element. The phenomenon of electric current rectification wholly takes place in the barrier layer which, in this particular plate, has been strengthened in the reverse direction but with the minimum adverse change in the forward direction. The static characteristic of a metal rectifier is determined by applying, first, d-c voltage in the forward direction (from zero to one, two, or three volts) and recording the d-c current per unit area (milliamperes per square centimeter); and second, by applying d-c voltage from zero to maximum plate voltage in the reverse direction and measuring the current leakage in milliamperes per unit area.

There is a total of six arms in the rectifier of the three-phase bridge circuit, but only two arms function during each one sixth of a cycle. The remaining four arms are blocking, Figure 2*A*. Since the total d-c output is greater than 12.5 amperes, several plates are placed in parallel. The rectifier of 200-ampere 30-volt rating has a total connection of 6–1–16, and the figure 16 designates the number of parallel plates in each arm of the bridge circuit. Physically all 96 plates are assembled into three stacks, interconnected with suitable bus structure and are housed in a lightweight case designed and built in accordance with usual aircraft practice, Figures 3 and 4.

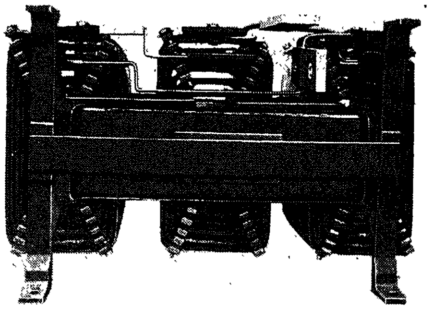
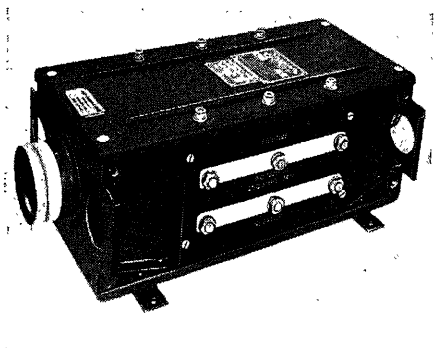


Figure 6. Aircraft transformer, air-cooled, 33.7-kva 400-cycle three-phase, 250-volt primary winding, 28-volt secondary winding

Weight 30 pounds. Front view. Over-all length ten inches



Figur 4. Aircraft rectifier, same as Figure 3, side covers open, ready for installation of conduits and cables

The upper section of Figure 2*B* illustrates the function of the selenium plate in the passing or forward direction. There is a certain amount of voltage drop per plate. This drop varies with the current density or the total output of the rectifier and constitutes forward losses. In a rectifier giving an output of 200 amperes, its value is in the neighborhood of 2.3 to 2.7 volts. This drop, however, changes with the time of service of the rectifier. Its increase during 500 hours of continuous operation may bring a net result of five per cent drop in the d-c output with the same a-c input to the rectifier. For some time, it had been expected that an idle condition of the rectifier would give a marked increase in this voltage drop. To date, however, observations have shown that this is not the case, although some momentary deforming of selenium plates may be expected after long idleness.

With the two arms of the bridge circuit passing the current in forward direction, the remaining four arms are blocking. An ideal rectifier would block reverse current completely. Actually, however, there is a certain amount of current leakage. Its value depends on the operating temperature to some extent but mainly on the length of service of the rectifier itself. The lower curve of Figure 2*B* illustrates a typical leakage current characteristic at an ambient temperature from 10 to 40 degrees centigrade. At both ends of this temperature range, the leakage current increases. Under all temperature conditions, however, the currrent leakage decreases with the life of the rectifier. The curve represents a typical reverse voltage-current density

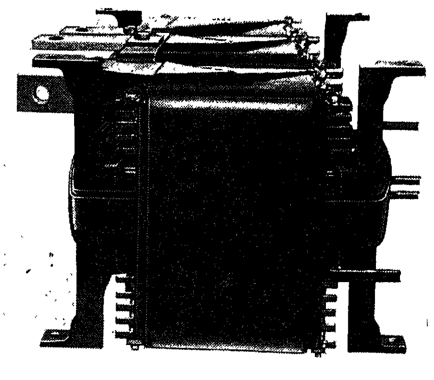


Figure 7. 33.7-kva three-phase air-cooled transformer, same as Figure 6

Side view. Over-all width seven inches

relation observed with two plates opposing each other. In the actual three-phase rectifier circuit, the reverse current is somewhat higher than that shown in Figure 2*B*, since the effects of all plates are integrated over the entire cycle. Computations and subsequent tests showed that the total current leakage of a 200-ampere rectifier is approximately 2.4 amperes at the start of service and is in the neighborhood of 1.6 amperes after 500 hours of continuous operation.

The necessary a-c input voltage to give the required direct current output voltage is computed by means of already established formulas.[4-6]

$V_{ac}=0.74\times30+2\times1\times2.7=27.6$ volts

The performance of this rectifier is illustrated in Figure 5.

Transformer

In the design of the transformer, full advantage was taken of improved materials and latest methods. Following the general aircraft practice of obtaining minimum weight and highest efficiency, high-permeability low-loss silicon steel was selected for the core structure.[7,8] Since the transformer was to operate under conditions of high magnetic induction, this type of steel, insulated with a silicate glass and processed under a rigidly controlled technique of heat treating and crystal orientation, was to give maximum amount of flux, some 40 per cent more than the best transformer steel, with a remarkably small external magnetizing force. Furthermore, this material permits not only lower total losses, including copper losses, but also their better balance, greater short-time overload capacity, lower impedance, and better voltage regulation. The latter measured 12 per cent and with the flux density of 16,150 lines per square centimeter and resulting core losses of 320 watts at 350 cycles per second, the efficiency of the 33.7-kva transformer was 95 per cent at full load. As the frequency increases, the core losses decrease. Even with the frequency 25 per cent higher, when the flux density was in the neighborhood of 12,800 lines per square centimeter, the core losses decreased almost 40 per cent. At these high frequencies, the machined butt joints also added to the high performance of the transformer.

Both windings were designed with a view to keeping copper losses to the minimum. This has been accomplished with the use of a rectangular wire for the primary windings and with the use of very thin and wide flat copper strips for the secondary windings. In this manner, the radiating area of the copper was increased markedly, and, since there is a certain amount of skin effect in the conductor under the conditions of high frequency and high current, this feature helped greatly in keeping copper losses down. With the current density of 6,600 amperes per square inch in the primary winding conductor and some eight per cent less in that of the secondary, the copper losses at 75 degrees centigrade were only 620 watts and 610 watts for primary and secondary windings, respectively.

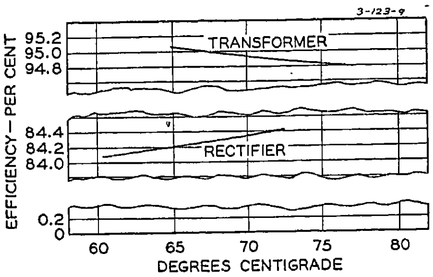


Figure 9. Characteristics illustrating the effect of temperature upon efficiencies of transformer and rectifier

All other material employed in this transformer was also selected with the sole objective of obtaining optimum performance. The winding, for instance, is separated from the core by means of heat-resistant glass cloth-base laminated plastic. The ventilating ducts in the winding are formed by means of sticks made from the glass cloth-base plastic. The frame itself is made of four corner supports welded from pieces of high-strength aluminum alloy. The supports are clamped to the core with a steel tape in such a manner that vibration stresses are taken by the supports rather than by the entire transformer structure. This type of frame construction contributed greatly in achieving lightness of weight, Figures 6 and 7.

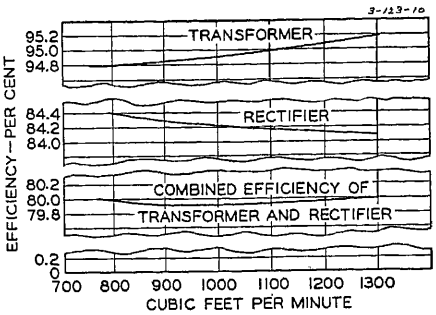


Figure 10. Transformer and rectifier efficiency characteristics as affected by the amount of cooling air

The secondary winding, carrying approximately 375 amperes, is spirally wound and has a-c resistance much lower than the conventional type of winding. The actual amount of 650 watts of copper losses in such a winding at 1,000 cycles per second frequency and 150 degrees Fahrenheit operating temperature would be some six times as great if the same winding were made of a square wire, of the identical cross-section area and arranged in a single layer. Furthermore, the copper-foil winding facilitates cooling, since the thin and wide strip has a surface area of almost six square inches per inch of conductor length, whereas, the square wire conductor has a surface of one square inch per each inch of conductor length.

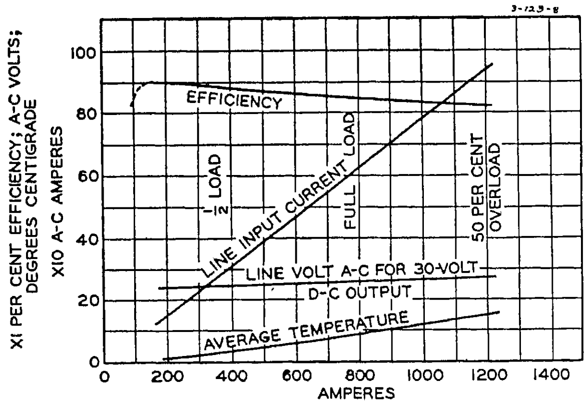


Figure 8. Performance characteristics of 800-ampere 30-v lt rectifier used in Figure 1 system

Alternator

During the past year, several alternators were designed, built, and tested. Two voltage ratings (250 and 28 volts, line) were tried. The alternator used in Figure 1 experiment was of 35-kva 250-volt rating. Two more alternators became available. One is 16 kva to operate with the rectifier delivering 400-ampere 30-volt output; another of still lower kilovolt-ampere rating, Figure 11.

This aircraft alternator is rated at

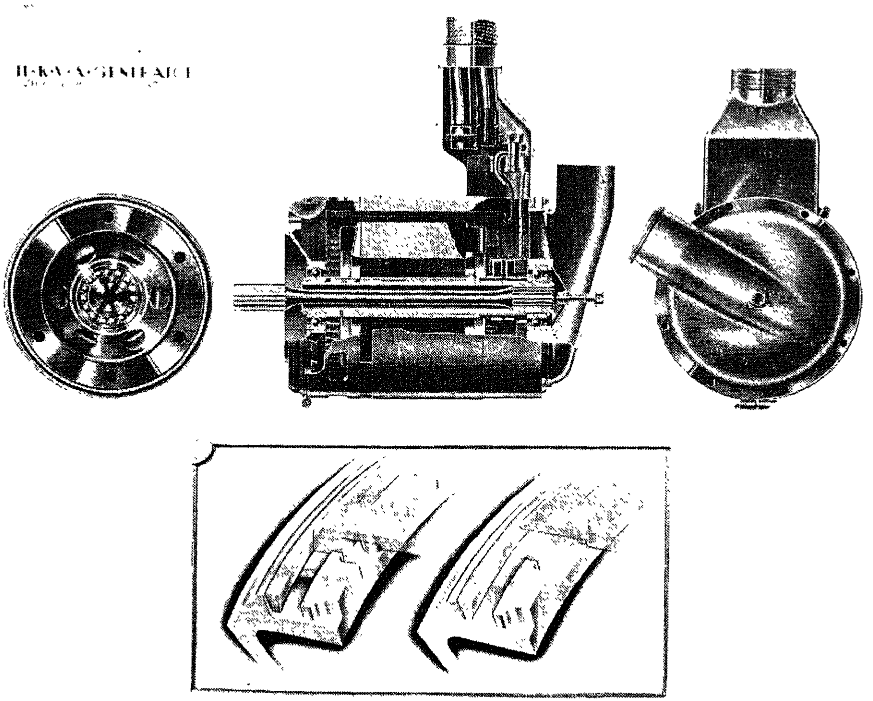


Figure 11. 11-kva three-phase air-cooled alternator, weight 33 pounds

Top, from left to right:

1. Engine pad view of the alternator
2. Section through alternator
3. Cooling cap and terminal block

Bottom, enlarged details of bayonet-type mounting flange. On left, alternator initially placed on flange. On right, alternator in locked position

11 kva, three phase, 30 volts, 211 amperes, 440 cycles and is capable of delivering 10.45 kw at 95 per cent power factor and at the minimum speed of 4,400 revolutions per minute. It was designed for a field current which could be handled by the 28-volt 200-ampere voltage regulator normally available; therefore, it does not represent the lightest possible design which could be achieved by a heavier field current (12 amperes) and a special regulator. Its approximate weight is 33 pounds.

The stator is delta-connected with two parallel circuits per phase. There are 72 semienclosed slots with two slots per pole per phase and full pitch winding. Rectangular wire is used in the stator winding to obtain maximum ratio of copper to slot area. Paper insulation is used since the alternator temperature rise is kept within class *A* insulation limits. The 12-pole rotor is skewed. Metallic slot wedges are used for the rotational speeds encountered. The field coil ends are strapped to metallic reinforcing rings.

Forced draft cooling is employed. A six-inch total pressure differential is required with a two-inch blast pipe. The internal air passages are so arranged that reverse flow of cooling air is caused, and thus more efficient use is made of the available air.[9]

800-Ampere 30-Volt Electric System

The first unit (Figure 1) with rectifier having operating characteristics shown in Figure 8 and supplying a total output of 24.6 kw, 0.6 kw of which was used by the field circuit of the alternator, has been tested under ordinary laboratory conditions and has given an over-all efficiency of 72 per cent. The efficiencies of the transformer and rectifier are affected by the amount of cooling air and, therefore, by the operating temperature of the respective units, Figures 9 and 10. The amount of cooling air was varied from 750 cubic feet per minute to 1,300 cubic feet per minute. Over this range of volume of cooling air, the combined efficiency of the transformer rectifier unit remained practically constant, since the efficiency of the transformer decreases and that of the rectifier increases with the temperature rise. It follows then that the minimum volume of air can be chosen to maintain both components within their safe temperature limits.

High-Altitude Operations

The high-altitude operations constitute unusual conditions of rarefied air and extreme low temperature. The 200-ampere rectifier unit without transformer has been tested in a stratosphere chamber under 45,000 feet altitude and minus 43 degrees centigrade temperature conditions.

The results could readily be analyzed in the light of the same characteristics shown in Figures 9 and 10. Referring to Figure 9, it can be seen that rectifier efficiency decreases steadily as the temperature goes down. At the same time, however, efficiency increases with the reduction of the cooling air as a result of the higher internal temperature of the rectifier. The net result is that both effects tend to balance each other and to maintain the efficiency of the rectifier at a point governed by percentage of loading.

Conclusion

It is gratifying to comment here that at present a large amount of work is being carried on by capable engineers and manufacturers connected with or serving the aviation industry. The results no doubt will bring about the availability of several flexible electric systems from which to choose one meeting particular requirements.[9]

References

1. APPLICATIONS OF ELECTRIC POWER IN AIRCRAFT, T. B. Holliday. ELECTRICAL ENGINEERING, volume 60, May 1941, pages 218-25.

2. OPTIMUM VOLTAGE FOR AIRPLANES, V. H. Grant, M. F. Peters, ELECTRICAL ENGINEERING, volume 58, October 1939, pages 428-31.

3. AIRCRAFT ELECTRICITY AS THE AIRLINE OPERATOR SEES IT, P. C. Sandretto. Society of Automotive Engineers, *Journal*, volume 48, number 4, April 1941, pages 154-9.

4. SELENIUM RECTIFIERS AND THEIR DESIGN, J. E. Yarmack. AIEE TRANSACTIONS, volume 61, 1942, July section, pages 488-95.

5. THE CHARACTERISTICS AND APPLICATIONS OF THE SELENIUM RECTIFIER, E. A. Richards, *Journal* of the Institution of Electrical Engineers (London, England), volume 88, part III, number 4, December 1941, pages 238-57.

6. SELENIUM RECTIFIERS FOR CLOSELY REGULATED VOLTAGES, J. E. Yarmack. *Electronics*, September 1941, pages 46-9; *Electrical Communication*, volume 20, number 2, 1941, pages 124-31.

7. HIPERSIL, A NEW MAGNETIC STEEL AND ITS USE IN TRANSFORMERS, J. K. Hodnette, C. C. Horstman. *Westinghouse Engineer*, August 1941, pages 52-6.

8. BETTER MAGNETIC CORE STEEL AIDS TRANSFORMER ENGINEER, C. C. Horstman. *Product Engineering*, January 1943, pages 28-30.

9. PRINCIPLES OF AERONAUTICAL RADIO ENGINEERING, Chapter XI, P. C. Sandretto. McGraw Hill Book Company, Inc., New York, N. Y.

Formulas for Calculating Short-Circuit Forces Between Conductors of Structural Shape*

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THE mechanical forces acting on bus-bar supports during short circuit are determined both by the magnitudes of the mutual electromagnetic forces exerted among the bus conductors and by the elastic properties of the supporting structure: its motional resistance, natural frequencies, and physical nature and arrangement. Comprehensive mathematical studies,** substantiated by experimental data, of the individual and collective effects of these factors and general methods for calculating the magnitudes involved have been advanced by Schurig and Sayre,[1] Dahlgren,[2,3] and Pilcher.[4] In shorter articles based on the work of the first-named Schurig, Fricke and Sayre,[5] Tanberg,[6] Specht,[7] Edson,[8] and others[9] have presented certain charts, nonograms, and short cuts that facilitate the numerical labor incident to actual calculation.

Now though the circumstances of a particular problem may render certain of these methods more advantageous to use than the others, *all require calculation of the mutual electromagnetic forces exerted among the conductors of the bus.* Commonly the bus is comprised of a group of long linear parallel nonmagnetic cylindrical conductors. If so, calculation of the electromagnetic force acting on a specified conductor resolves into successive determination of the magnitudes and phase relationships of the currents in the conductors, of the mutual force exerted between the given conductor and each of the others, and of appropriate vector summation of these last.

Calculation of the actual short-circuit currents requires use of symmetrical components. General procedures specifically adapted to short-circuit calculation of industrial-plant distribution systems, together with inclusive pertinent numerical data (electrical constants of standard types of conductor, of typical busses, and of the usual attached electrical apparatus), are encompassed in elaborate works by Hanna[10] and by Darling.[11,12] Illustratively, Schulze[13] has given a detailed numerical analysis of the short-circuit calculation of a typical plant distribution system. Other phases of the problem of calculation of the short-circuit currents have been discussed by Papst,[14] Schurig,[15] Leonard and Riker,[16] Gross,[17] Robinson,[18] Tripp,[19] and Kilian.[20,21]

However, between finding the short-circuit currents (for which step are available all the methods and data contained in the papers mentioned in the preceding paragraph) and summing the forces exerted on the specified conductor by the other conductors (which step for a d-c bus requires but simple arithmetic and for an a-c bus the only slightly less simple task of adding complex numbers) is the intermediate and important task of *finding these individual forces.* Theoretically, if the conductor currents and geometry are known, these forces can be calculated by use of fundamental electromagnetic theory. Practically, the mathematical analysis requisite to use of this theory is far from simple—if not actually intractable—and the bus designer turns, perforce, to the use of known formulas or, if these latter be lacking, to some type of approximation, the accuracy of which is more or less indeterminate.

Obviously, resort to such approximation, certainly inconsistent with good engineering practice, is most undesirable. Yet there is little choice to the contrary. For, though the mechanical and electrical advantages stemming from the use of linear conductors of structural shape are so marked that, at present, most busses for heavy-current duty are constructed of such conductor; and, though standard designs of single and polyphase busses utilize strap, rectangular, tubular, channel, angle, tee, and I-beam conductor, a recent exhaustive survey[22] of the literature reveals, seemingly, that explicit formulas for calculating the electromagnetic forces between two conductors of the shapes mentioned are virtually nonexistent. A paper by Dwight[23] contains formulas and curves for calculating the force between two identical conductors having parallel coplanar axes and parallel-sided rectangular cross sections. And papers by Higgins[24,25] contain formulas for calculating the force between two identical conductors having parallel coplanar axes and parallel-sided full or hollow rectangular cross sections. But, despite extended and increasing use, there have not been published explicit formulas for calculating the force between conductors (not necessarily identical) of parallel-sided full or hollow rectangular conductors arranged other than just described; or for any arrangement of conductors of angle, channel, tee, or *I*-beam shape; or for any arrangement of composite conductors constructed of combinations of two or more of these six shapes.

Such formulas are derived in this paper, it being assumed that the conductors are nonmagnetic, are of such length that end effects are negligible, are right cornered, and carry current distributed uniformly over their cross sections.

Of these four assumptions, the first and second are commonly satisfied in practice. The third and fourth assumptions, though not always true in practice (for example, structural shapes built up of strap conductor are usually right cornered, whereas structural shapes drawn or rolled in one piece usually have slightly

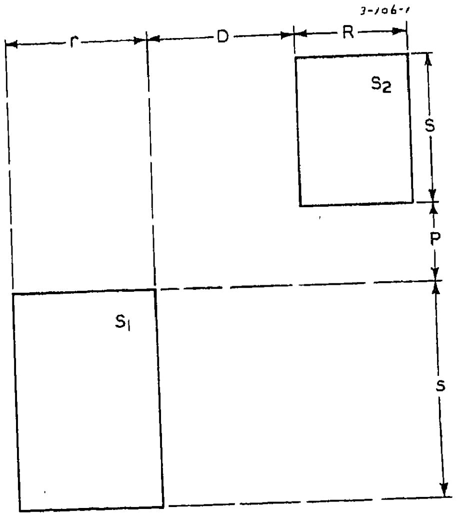

Figure 1

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* As the formulas pertinent to conductors of full or annular circular cross section are well known, they are not treated in this paper.

** While the work of the first named is frequently cited in the American literature, that of the other two, seemingly, has attracted but little attention. Yet Pilcher's work is an extension of the theory delineated by Schurig and Sayre; while Dahlgren's, an extended study of the important phenomenon of mechanical resonance, complements both.

rounded corners; again, skin and proximity effects are always present on a-c busses, though, as Beetz[26] has discussed at length, at power frequencies and normal bus spacings the effect of these phenomena on the magnitudes of the electromagnetic forces is vanishingly small) introduce error negligible with reference to the accuracy required in calculating short-circuit forces and enable solution of an otherwise intractable problem.

If these four postulates are granted, derivation of the desired formulas is not particularly difficult. First, through application of fundamental electromagnetic theory specific formulas are obtained for the components of the electromagnetic force exerted between two conductors, the cross sections of which are *arbitrarily located* parallel-sided rectangular areas. Then these formulas and the same electromagnetic principles are conjoined to obtain general formulas for the components of the force exerted between two conductors, the cross sections of which are arbitrary configurations comprised of parallel-sided rectangular areas (section I). Next, the theory essential to calculation of the derivatives of the geometric mean distances occurring in these formulas is developed (section II). Finally, as the general formulas of section I are applicable to an inclusive category that embraces conductors of structural shape, explicit formulas for the components of the force exerted between conductors of specified structural shape can be deduced from these general formulas as desired (section III).

I. General Formulas for the Components of the Electromagnetic Force

Invoking well-known theory, the inductance L of a circuit comprised of two conductors with rectangular cross sections S_1 and S_2 disposed as in Figure 1 is

$$L=2\log(D_{12}^2/D_{11}D_{22})$$

$$=-(2/I^2)\sum_{i=1}^{2}\sum_{j=1}^{2} w_iw_jS_iS_j\log D_{ij} \qquad (1)$$

wherein I is the magnitude of the circuit current; w_i and w_j are, respectively, the current densities in S_i and S_j, taken positive for current in one direction, negative for current in the other direction (whence w_i^2 and w_j^2 are positive, w_iw_j positive or negative depending on the relative direction of the two currents in S_i and S_j); and D_{ij} is the geometric mean distance of S_i from S_j.

The energy in the magnetic field associated with the circuit is $W=LI^2/2$; whence equation 1 yields

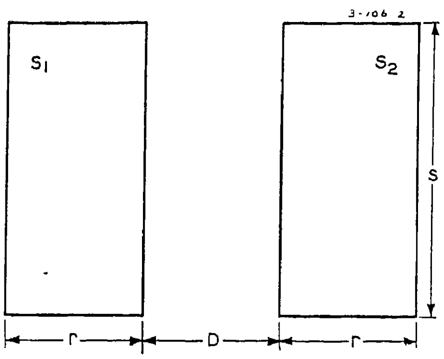

Figure 2

$$W=-\sum_{i=1}^{2}\sum_{j=1}^{2} w_iw_jS_iS_j\log D_{ij} \qquad (2)$$

As is well known, the rectangular components of the electromagnetic force exerted between the two conductors are given by

$$f_x\equiv f_D=\partial W/\partial D;\; f_y\equiv f_P=\partial W/\partial P \qquad (3)$$

Substituting from equation 2 in equation 3 as indicated yields

$$f_D=-\sum_{i=1}^{2}\sum_{j=1}^{2} w_iw_jS_iS_j\partial(\log D_{ij})/\partial D \qquad (4)$$

$$f_P=-\sum_{i=1}^{2}\sum_{j=1}^{2} w_iw_jS_iS_j\partial(\log D_{ij})/\partial P \qquad (5)$$

If each of the two conductors is comprised of several subconductors connected in parallel, one comprised of m subconductors numbered from 1 to m and the other of n subconductors numbered from $m+1$ to $m+n$, equation 23 of reference 27 yields for the inductance of the circuit

$$L=-(2/I^2)\sum_{i=1}^{m+n}\sum_{j=1}^{m+n} w_iw_jS_iS_j\log D_{ij} \qquad (6)$$

As equation 6 is identical with equation 1 except for the difference in the upper limits of the summations, we obtain from equations 4 and 5 by analogy

$$f_D=-\sum_{i=1}^{m+n}\sum_{j=1}^{m+n} w_iw_jS_iS_j\,\partial(\log D_{ij})/\partial D \qquad (7)$$

$$f_P=-\sum_{i=1}^{m+n}\sum_{j=1}^{m+n} w_iw_jS_iS_j\,\partial(\log D_{ij})/\partial P \qquad (8)$$

wherein f_D and f_P are the component forces on a conductor or a specified subconductor according as D and P are parameters locating one conductor with respect to the other or are parameters locating the subconductor with respect to the other $(m+n-1)$ subconductors.

If we have a double circuit comprised of $p+q$ conductors completely *in toto*, one circuit of two conductors being comprised of p subconductors numbered from 1 to p and the other of q subconductors numbered from $p+1$ to $p+q$, the two circuits carrying currents I_p and I_q respectively, equation 26 of reference 27 yields for the mutual inductance of the two circuits

$$M=-(1/I_pI_q)\Bigg[\sum_{i=1}^{p+q}\sum_{j=1}^{p+q} w_iw_jS_iS_j\log D_{ij}-$$

$$\sum_{i=1}^{p}\sum_{j=1}^{p} w_iw_jS_iS_j\log D_{ij}-$$

$$\sum_{i=p+1}^{p+q}\sum_{j=p+1}^{p+q} w_iw_jS_iS_j\log D_{ij}\Bigg] \qquad (9)$$

As the second term within the braces is a function only of the parameters defining the geometry and relative location of the cross sections of the conductors comprising the circuit carrying I_p; as a similar statement is true for the third term; and as, therefore, both terms are independent of the parameters, say D' and P', used to locate the conductors of one circuit relative to the other, differentiation of the right-hand member of equation 9 yields

$$f_{D'}=-\sum_{i=1}^{p+q}\sum_{j=1}^{p+q} w_iw_jS_iS_j\,\partial(D_{ij})/\partial D' \qquad (10)$$

$$f_{P'}=-\sum_{i=1}^{p+q}\sum_{j=1}^{p+q} w_iw_jS_iS_j\,\partial(D_{ij})/\partial P' \qquad (11)$$

wherein $f_{D'}$ and $f_{P'}$ are the component forces exerted on a bus, a conductor of the bus, or a subconductor of the bus, according as P' and D' are parameters used to locate the bus, a conductor of the bus, or a subconductor of the bus from the like elements of the other bus. If, then, to the component force on a specified conductor or subconductor obtained from equations 10 or 11 we add the corresponding value obtained from equations 7 or 8, which value is the force exerted on the conductor or subconductor by the other like elements of the *same* circuit, we obtain the total force on the specified conductor or subconductor.

In an n-phase system comprised of n distinct conductors, one conductor, say a, can be considered as carrying current I_a (the currents are expressed as complex

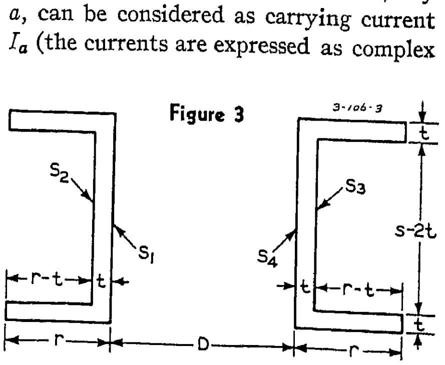

Figure 3

numbers) and the remaining $(n-1)$ conductors can be considered as connected in parallel and carrying the return current. Accordingly, our problem is interpretable as a single-phase circuit comprised of two conductors, one full, the other divided, whence we have from equations 7 and 8

$$f_D = -I_a \sum_{i=b}^{n} I_i\, \partial(\log D_{ai})/\partial D \qquad (12)$$

$$f_P = -I_a \sum_{i=b}^{n} I_i\, \partial(\log D_{ai})/\partial P \qquad (13)$$

wherein P and D are parameters locating S_a with respect to $S_b, \ldots, S_n$.

If the individual conductors are divided, a composed of a' subconductors numbered from 1 to a', b of b' subconductors numbered from $a'+1$ to $a'+b'$, and correspondingly for the others, we have from equations 7 and 8

$$f_D = -\sum_{i=1}^{a'} \sum_{j=1}^{a'+\ldots+n'} w_i w_j S_i S_j\, \partial(\log D_{ij})/\partial D \qquad (14)$$

$$f_P = -\sum_{i=1}^{a'} \sum_{j=1}^{a'+\ldots+n'} w_i w_j S_i S_j\, \partial(\log D_{ij})/\partial P \qquad (15)$$

where now f_D and f_P give the component forces on the entire conductor a or on any subconductor of a, say a', according as D and P are parameters locating conductor a with respect to the other $n-1$ conductors, or are parameters locating subconductor a' with respect to the other $a'+\ldots+n'-1$ subconductors.

With regard to the physical quantities mentioned to this point, all units are in the absolute system: linear dimensions in centimeters, current I in abamperes; self inductance L and mutual inductance M in abhenrys per centimeter of bus length; energy W in ergs per centimeter of bus length; force f in dynes per centimeter of bus length.

Reviewing the analysis of this section, we see that in each case treated the total force, $f = (f_D^2 + f_P^2)^{1/2}$, is determined in both magnitude and direction providing the derivatives indicated in the summations are expressible as known functions of the dimensions of the conductors and bus. Accordingly, we turn now to development of formulas for calculating these derivatives.

II. Calculation of the Derivatives of the Geometric Mean Distances

In earlier papers the writer[27,28] has derived the geometric mean distance D_{12} of a rectangular area S_1 from a second arbitrarily located parallel-sided rectangular area S_2, dimensioned as in Figure 1, to be

$$S_1S_2 \log D_{12} = -(25/12)S_1S_2 - (1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}K(A_i, B_j) \qquad (16)$$

wherein

$$K(A_i, B_j) = (A_i^4 - 6A_i^2B_j^2 + B_j^4)\times \log(A_i^2+B_j^2) + 4A_iB_j(B_j^2 - A_i^2)\times \tan^{-1}(B_j/A_i) - 2\pi A_iB_j \qquad (17)$$

and

$$A_1 = |D+R+r|;\ A_2 = |D+R|;\ A_3 = |D|;\ A_4 = |D+r| \qquad (18)$$

$$B_1 = |P+S+s|;\ B_2 = |P+S|;\ B_3 = |P|;\ B_4 = |P+s| \qquad (19)$$

The parameter D in equation 18, and similarly P in equation 19, may be positive, zero, or negative. Thus, if $r=R$, $s=S$, $D=-r$, and $P=-s$, the two areas S_1 and S_2 are identical and superimposed, and equation 16 yields the formula for the self geometric mean distance of a rectangular area. Other more detailed illustrations of application of equation 16 to the determination of the geometric mean distance between two variously located rectangular areas are contained in section V of reference 27 and in section III of this paper.

Turning to calculation of the derivatives indicated in the summations of the preceding section, we have from equation 16 that

$$S_1S_2\, \partial(\log D_{12})/\partial D = -(1/24)\times \sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}\partial K(A_i, B_j)/\partial A_i dA_i/dD \qquad (20)$$

From equation 17

$$\partial K(A_i, B_j)/\partial A_i = 4A_i(A_i^2 - 3B_j^2)\times \log(A_i^2+B_j^2) + 4B_j^2(B_j^2 - 3A_i^2)\times \tan^{-1}(B_j/A_i) + A_i^3 - 3A_iB_j^2 - 2\pi B_j^3 \qquad (21)$$

From equation 18

$$dA_i/dD = d|D + \text{constant}|/dD = \pm 1 \qquad (22)$$

the plus or the minus sign to be chosen according as in a particular problem the specified value of D is such that the quantity within the bars is positive or is negative. Finally, substituting equations 21 and 22 in equation 20 yields the desired formula

$$S_1S_2\, \partial(\log D_{12})/\partial D = -(1/24)\times \sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}[4A_i(A_i^2 - 3B_j^2)\times \log(A_i^2+B_j^2)^{1/2} + 4B_j(B_j^2 - 3A_i^2)\times \tan^{-1}(B_j/A_j) + A_i^3 - 3A_iB_j^2 - 2\pi B_j^3]dA_i/dD \qquad (23)$$

Certain variants of equation 23 are of value. If dA_i/dD is of the same sign for $i=1$ to 4, then

$$\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}A_i^3 dA_i/dD = 2\pi\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}B_j^3 dA_i/dD = 0 \qquad (24)$$

whence equation 23 reduces to

$$S_1S_2\, \partial(\log D_{12})/\partial D = -(1/24)\times \sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}[4A_i(A_i^2 - 3B_j^2)\times \log(A_i^2+B_j^2)^{1/2} + 4B_j(B_j^2 - 3A_i^2)\times \tan^{-1}(B_j/A_i) - 3A_iB_j^2]dA_i/dD \qquad (25)$$

Further, if $dA_i/dD = 1$ for $i=1$ to 4, then

$$3\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+} A_iB_j^2 dA_i/dD = 0 \qquad (26)$$

whence, in turn, equation 25 reduces to

$$S_1S_2\, \partial(\log D_{12})/\partial D = -(1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1) \quad U(A_i, B_j) \qquad (27)$$

wherein, for convenience of future reference,

$$U(A_i, B_j) = 4A_i(A_i^2 - 3B_j^2)\log(A_i^2 + B_j^2)^{1/2} + 4B_j(Bj^2 - 3A_i^2)\tan^{-1}(B_j/A_i) \qquad (28)$$

By virtue of the identity, $K(A_i, B_j) = K(B_j, A_i)$ (to be obtained from equation 17 by interchange of A_i with B_j and subsequent use of the trigonometric identity, $\tan^{-1}(A_i/B_j) + \tan^{-1}(B_j/A_i) = \pi/2$), corresponding expressions for $S_1S_2\partial\times(\log D_{12})/\partial P$ follow from those for $S_1S_2\partial(\log D_{12})/\partial D$ through interchange of A_i and B_j and replacement of D by P. As this operation is easily effected, for economy of space we here set forth only the analog of equation 27:

$$S_1S_2\, \partial(\log D_{12})/\partial P = -(1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}(VA_i, B_j) \qquad (29)$$

wherein

$$V(A_i, B_j) = 4B_j(B_j^2 - 3A_i^2)\log(A_i^2 + B_j^2)^{1/2} + 4A_i(A_i^2 - 3B_j^2)\tan^{-1}(A_i/B_j) \qquad (30)$$

Equations 16 to 30 inclusive suffice for calculation of the derivatives occurring in the double summations defining f_D and f_P. Accordingly, inasmuch as for any given set of data the components f_D and f_P are completely determinable through use of the analysis developed to this point, it follows that this analysis comprises the general solution of the problem of calculating the electromagnetic force, $f = (f_D^2 + f_P^2)^{1/2}$, exerted on a specified conductor or subconductor of a d-c single-phase or polyphase bus comprised of conductors the cross sections of which are arbitrary configurations of parallel-sided rectangular areas.

In general the double summation indicated in the right-hand member of each of equations 23, 25, and 27 is calculated by inserting the numerical values of the parameters A_i and Bj (determined from equations 8 and 9 respectively), obtaining the indicated logarithms and arc tangents from suitable tables, and performing the necessary arithmetic. If, however, it happens—and this case is the rule rather than the exception—that the bus and conductor geometry is such that equation 27 is valid and, further, that therein the summation to be effected is of a form typified by

$$\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}U(A_i, B_j) = \sum_{j=1}^{4}(-1)^{j}F(D, r, B_j) \quad (31)$$

whereof, typically,

$$F(D, r, s) = 2U(D, s) - U(D+r, s) - U(D-r, s) \quad (32)$$

the U functions being those defined by equation 28, the numerical computation can be effected more quickly and with less chance of numerical error through use of the rapidly converging Taylor's series in which the right-hand member of equation 32, considered as a function of D, can be expanded about the point $D=r$; that is, by

$$F(D, r, s) = 12r^2[2s \tan^{-1}(s/D) - D\times \log(D^2+s^2) + (20/12) - Dr^2/6(D_2+s^2) - Dr^4(D^2-3s^2)/90(D^2+s^2)^3 - Dr^6(D^4-10D^2s^2+5s^4)/420(D^2+s^2)^5 - \ldots] \quad (33)$$

The details of this expansion are given in the appendix. Substituting appropriately from equation 33 in equation 31, noting that in the resulting expansion

$$\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}(20/12) = 0$$

simplifying, and then substituting in equation 27 we have

$$S_1S_2\, \partial(\log D_{12})/\partial D = (-1/2)\sum_{j=1}(-1)^{j}G(D, r, B_j) \quad (34)$$

wherein

$$G(D, r, B_j) = Dr^2[2n \tan^{-1} n - \log(D^2+B_j^2) - m^2/6(1+n^2) - m^4(1-3n^2)/90(1+n^2)^3 - m^6(1-10m^2n^2+5n^4)/420(1+n^2)^5 - \ldots] \quad (35)$$

and $m=r/D$, $n=B_j/D$.

Analogously, if equation 27 holds, we have from equation 28

$$S_1S_2\, \partial(\log D_{12})/\partial P = -(1/2)\sum_{i=1}^{4}(-1)^{i}H(P, A_i, s) \quad (36)$$

wherein

$$H(P, A_i, s) = Ps^2[2m \tan^{-1} m - \log(P^2+A_i)^2 - n^2/6(1+m^2) - n^4(1-3m^2)/90(1+m^2)^3 - n^6(1-10n^2m^2+5m^4)/420(1+m^2)^5 - \ldots] \quad (37)$$

and now $m=A_i/P$, $n=s/P$.

Again, it often happens in practice that the right-hand member of equation 34 reduces to, typically,

$$\sum_{j=1}^{4}(-1)^{j}G(D, r, B_j) = -2G(D, r, s) + 2G(D, r, 0) \quad (38)$$

If so, by virtue of equations 35 and 38, we have from equation 34 the very convenient form

$$S_1S_2\, \partial(\log D_{12})/\partial D = DM(D, r, s) \quad (39)$$

where $M(D, r, s)$ is defined by

$$M(D, r, s) = r^2[2n \tan^{-1} n - \log(1+n^2) + m^2n^2/6(1+n^2) + \ldots] \quad (40)$$

and $m=r/D$, $n=s/D$.

Analogously, from equation 37 we have

$$S_1S_2\, \partial(\log D_{12})/\partial P = PN(P, r, s) \quad (41)$$

where $N(P, r, s)$ is defined by

$$N(P, r, s) = s^2[2m \tan^{-1} m - \log(1+m^2) + n^2m^2/6(1+m^2) + \ldots] \quad (42)$$

and now $m=r/P$, $n=s/P$.

III. Some Illustrative Examples

Example 1 is advanced as corroborative of the preceding analysis. Example 2 is advanced as demonstrative of application of this analysis to calculation of the electromagnetic force exerted between two conductors of structural shape.

EXAMPLE 1

A single-phase bus is comprised of two identical strap conductors (Figure 2). To calculate the electromagnetic force per unit length:

For this case: $r=R$; $s=S$; $D=D$; $P=-s$; $S_1=S_2=rs$; $w_1=-w_2=I/S_1=I/rs$. Then equations 18, 19, and 22 yield $A_1=D+2r$; $A_2=A=D+r$; $A_3=D$; $B_1=B_3=S$; $B_2=B_4=0$; $\partial A_i/\partial D=1$, $i=1, 2, 3, 4$. From equation 4, noting that the self geometric mean distances, D_{11} and D_{22}, are independent of the parameter D,

$$\begin{aligned} f_D &= -\sum_{i=1}^{2}\sum_{j=1}^{2}w_iw_jS_iS_j\, \partial(\log D_{ij})/\partial D \\ &= -2w_1w_2S_1S_2\, \partial(\log D_{12})/\partial D \\ &= (2I^2/r^2s^2)S_1S_2\, \partial(\log D_{12})/\partial D \quad (43) \end{aligned}$$

By virtue of equations 27, 31, 32, 34, and 39 we obtain

$$\begin{aligned} S_1S_2\, &\partial(\log D_{12})/\partial D \\ &= -(1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}U(A_i, B_j) \\ &= -(1/24)\sum_{j=1}^{4}(-1)^{j}[2U(D+r, B_j) - U(D+2r, B_j) - U(D, B_j)] \\ &= -(1/24)\sum_{j=1}^{4}(-1)^{j}F(D+r, r, B_j) \\ &= -(1/2)\sum_{j=1}^{4}(-1)^{j}G(D+r, r, B_j) \\ &= G(D+r, r, s) - G(D+r, r, 0) \\ &= (D+r)M(D+r, r, s) \quad (44) \end{aligned}$$

wherein

$$M(D+r, r, s) = r^2[2n \tan^{-1} n - \log(1+n^2) + m^2n^2/6(1+n^2) + \ldots] \quad (45)$$

and $m=r/(D+r)$, $n=s/(D+r)$.

Finally, substituting from equation 44 in equation 43 we have

$$F_D = [2I^2/(D+r)][(D+r)^2M(D+r,r,s)/r^2s^2] \text{ dynes per centimeter of bus length} \quad (46)$$

or in units more commonly used: current I in amperes; D, r, s, in inches

$$f_D = [5.4I^2/(D+r)][(D+r)^2\times M(D+r, r, s)/r^2s^2]10^{-7} \text{ pounds per foot of bus length} \quad (47)$$

The dimensionless term in the braces, commonly represented by the symbol k_D, is termed the *electromagnetic space factor* for f_D.

By symmetry the component $f_p=0$. Accordingly, the total force, $f=f_D$, is given by equation 46 or 47. Allowing for the difference of notation, these formulas for f_D agree with those obtained earlier by H. B. Dwight[23] (who first considered this problem) and by the writer.[24]

EXAMPLE 2

A single-phase bus is comprised of two identical channels placed back to back (Figure 3). To calculate the electromagnetic force per unit length of bus:

Each of the two channels can be considered as comprised of two component conductors: one of full rectangular cross section wherein current exists in the same direction as in the channel, the other of smaller rectangular cross section having current in the opposite direction. Hence $S_1=S_4=rs$; $S_2=S_3=(r-t)(s-2t)$; $w_1=-w_2=w_3=-w_4=I/(S_1-S_2)=I/t(s-2r-2t)$.

From equation 7, noting that the self geometric mean distances, $D_{11}=D_{44}$ and $D_{22}=D_{33}$, are independent of the parameter D, we obtain

$$\begin{aligned} f_D &= -\sum_{i=1}^{4}\sum_{j=1}^{4}w_iw_jSiS_j\, \partial(\log D_{ij})/\partial D \\ &= [2I^2/t^2(s-2r-2t)^2]\,[S_1^2\,\partial(\log D_{14})/\partial D + S_2^2\,\partial(\log D_{23})/\partial D - 2S_1S_2\,\partial(\log D_{13})/\partial D] \quad (48) \end{aligned}$$

From equations 18, 19, and 22 we obtain for the three sets of (A_i, B_j) parameters

S_1, S_4: $A_1=D+2r$; $A_2=D+r$; $A_3=D$
$B_1=B_3=s$; $B_2=B_4=0$
S_2, S_3: $A_1=D+2r$; $A_2=A_4=D+r+t$; $A_3=D+2t$
$B_1=B_3=s-2t$; $B_2=B_4=0$
S_1, S_3: $A_1=D+2r$; $A_2=D+r+t$; $A_3=D+t$; $A_4=D+r$
$B_1=B_3=s-t$; $B_2=B_4=t$

The values of the first two derivatives in the right-hand member of equation 48 can be written down by analogy with the value found for the derivative in equation 43. Thus

$$S_1^2\, \partial(\log D_{14})/\partial D = (D+r)M(D+r, r, s) \qquad (49)$$

and

$$S_2^2\, \partial(\log D_{23})/\partial D = (D+r+t)M(D+r+t, r-t, s-2t) \qquad (50)$$

Calculation of the third derivative is effected in precisely the same fashion as in equation 44:

$$\begin{aligned} S_1S_2\, \partial(\log D_{13})/\partial D &= -(1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}U(A_i, B_j) \\ &= -(1/24)\sum_{j=1}^{4}(-1)^j([-U(D+2r, B_j)+ U(D+r+t, B_j)-U(D+t, B_j)+ U(D+r, B_j)] \\ &= -(1/24)\sum_{j=1}^{4}(-1)^j[-U(D+2r, B_j)- U(D+t, B_j)+2U(D+r+2^{-1}t, B_j)+ U(D+r+t, B_j)+U(D+R, B_j)- 2U(D+r+2^{-1}t, B_j)] \\ &= -(1/2)\sum_{j=1}^{4}(-1)^j[G(D+r+2^{-1}t, r- 2^{-1}t, B_j)-G(D+r+2^{-1}t, t/2, B_j)] \\ &= (D+r+2^{-1}t)[M(D+r+2^{-1}t, r-2^{-1}t, s- t)-M(D+r+2^{-1}t, 2^{-1}t, s-t)+ M(D+r+2^{-1}t, r-2^{-1}t, t)+ M(D+r+2^{-1}t, 2^{-1}t, t)] \qquad (51) \end{aligned}$$

Finally, substitution of equations 49, 50 and 51 in equation 48 yields the desired formula

$$f_D = [2I^2/t^2(s-2r-2t)^2]\{(D+r)M(D+ r,r,s)+(D+r+t)\,M(D+r+t, r-t, s- 2t)-(D+r+2^{-1}t)[M(D+r+2^{-1}t, r- 2^{-1}t, s-t)-M(D+r+2^{-1}t, 2^{-1}t, s- t)+M(D+r+2^{-1}t, r-2^{-1}t, t)+ M(D+r+2^{-1}t, 2^{-1}t, t)]\}\ \text{dynes per centimeter of bus length} \qquad (52)$$

wherein, typically, $M(D+r, r, s)$ is given by equation 45.

As by symmetry $f_P=0$, we have $f=f_D$. Inasmuch as the term, electromagnetic space factor, has significance—at least in the sense hitherto employed—only if each of the cross sections of the two conductors possess two principal axes of symmetry, k_D is not to be distinguished. Accordingly, equation 52 encompasses the complete solution of the problem.

Appendix

Consider as a function of D alone

$$F(D, r, s)=2U(D, s)-U(D+r, s)- U(D-r, s)$$

can be expanded in a Taylor's series about the point $D=r$. Accordingly, by Taylor's theorem we have

$$F(D, r, s)=-r^2\partial^2U(D, s)/\partial D^2- (r^4/2\cdot 3!)\partial^4U(D, s)/\partial D^4- (r^6/3\cdot 5!)\partial^6U(D, s)/\partial D^6- \ldots$$

wherein

$$\begin{aligned} U(D, s)&=4D(D^2-3s^2)\log(D^2+s^2)^{1/2}+ D(D^2-3s^2)\tan^{-1}(s/D) \\ \partial U(D, s)/\partial D&=6(D^2-s^2)\log(D^2+s^2)- 24Ds\tan^{-1}(s/D)+4(D^2-s^2) \\ \partial^2U(D, s)/\partial D^2&=12D\log(D^2+s^2)- 24s\tan^{-1}(s/D)+20D \\ \partial^3U(D, s)/\partial D^3&=12\log(D^2+s^2)-44 \\ \partial^4U(D, s)/\partial D^4&=24D(D^2+s^2)^{-1} \\ \partial^5U(D, s)/\partial D^5&=-24(D^2-s^2)(D^2+s^2)^{-2} \\ \partial^6U(D, s)/\partial D^6&=48D(D^2-3s^2)(D^2+s^2)^{-3} \\ \partial^7U(D, s)/\partial D^7&=-144(D^4-6D^2s^2+s^4)\times (D^2+s^2)^{-4} \\ \partial^8U(D, s)/\partial D^8&=576D(D^4-10D^2s^2+5s^4)\times (D^2+s^2)^{-5} \end{aligned}$$

Substitution of the derivatives in the preceding series yields

$$F(D, r, s)=12r^2[2s\tan^{-1}(s/D)-D\times \log(D^2+s^2)+(20/12)-Dr^2/6(D^2+s^2)- Dr^4(D^2-3s^2)/90(D^2+s^2)^3- Dr^6(D^4-10D^2s^2+5s^4)/420(D^2+s^2)^5- \ldots]$$

References

1. Mechanical Stresses in Bus-Bar Supports During Short Circuits, O. R. Schurig, M. F. Sayre. AIEE Transactions, volume 44, 1925, pages 217–37.

2. Elektrodynamiska Krafter hos Strömförande Ställverksskenor, F. Dahlgren. *Teknisk Tidskrift Elektroteknik*, (Stockholm, Sweden) [7], volume 55, 1925, pages 117–21.

3. Electrodynamical Forces in Bus Bars, F. Dahlgren. *Electrical World*, volume 86, 1925, pages 473–4.

4. Short-Circuit Forces on Bus Bars and Their Supports in Cellular Switchgear Structures, E. E. I. Pilcher. *World Power*, volume 24, 1935, pages 118–23, 190–2, 231–4.

5. Practical Calculation of Short-Circuit Stresses in Supports for Straight Parallel-Bar Conductors, O. R. Schurig, C. W. Frick, M. F. Sayre. *General Electric Review*, volume 29, 1926, pages 534–44.

6. Stresses in Bus Supports, R. Tanberg. *Electric Journal*, volume 24, 1927, pages 517–25.

7. Short-Cut Methods of Calculating Stresses in Bus Structures, W. Specht. *General Electric Review*, volume 31, 1928, pages 413–8.

8. Short-Circuit Stress Charts, W. M. Edson. *Electrical World*, volume 94, 1929, page 1125.

9. Bus Stresses Under Short Circuits. *Electrical World*, volume 92, 1928, pages 564–5.

10. Calculation of Short-Circuit Currents in A-C Networks, W. M. Hanna. *General Electric Review*, volume 40, 1937, pages 125–30; 189–96, 287–91; 383–8.

11. Short-Circuit Calculating Procedure for Low-Voltage A-C Systems, compiled by A. G. Darling. General Electric tract *GET*-1009, 1941.

12. Short-Circuit Calculating Procedure for Low-Voltage A-C Systems, A. G. Darling. AIEE Transactions, volume 60, 1941, pages 1121–36. Discussion, pages 1294–5.

13. Calculation of Fault Currents in Industrial Plants, R. C. Schulze. Electrical Engineering, volume 60, June 1941, pages 271–9.

14. Fault Currents Low With Heavy Power Capacity, H. W. Papst. *Electrical World*, volume 102, 1933, pages 208–9.

15. Fault Voltage Drop and Impedance at Short-Circuit Currents in Low-Voltage Circuits, O. R. Schurig. AIEE Transactions, volume 60, 1941, pages 479–87.

16. Repulsion Between Bus Bars, S. G. Leonard, C. R. Riker. *Electrical Journal*, volume 14, 1917, pages 491–3.

17. Theoretical Investigation of Electric Transmission Systems Under Short-Circuit Conditions, I. W. Gross. AIEE Transactions, volume 34, 1915, pages 23–67. Discussion, pages 68–84.

18. Magneto-Mechanical Loads on Bus Supports, L. N. Robinson. AIEE Transactions, volume 42, 1923, pages 1020–4. Discussion, pages 1025–8.

19. Forces on Conductors During Short Circuit, W. A. Tripp. *Electric Journal*, volume 34, 1927, pages 493–7.

20. Mechanical Forces on Busses Due to Fault Currents, S. C. Killian. *Electrical World* volume 118, 1942, pages 1938–40.

21. Forces Due to Short-Circuit Currents, S. C. Killian. *Delta-Star Magazine*, January–February, March–April, 1943.

22. Design of Direct-Current and Low-Frequency Bus Systems: A Guide to the Literature, T. J. Higgins. *Proceedings* of the Midwest Power Conference, 1943.

23. Repulsion Between Strap Conductors, H. B. Dwight. *Electrical World*, volume 70, 1917, pages 522–4.

24. Formulas for Calculating Short-Circuit Stresses for Bus Supports for Rectangular Tubular Conductors, T. J. Higgins. AIEE Transactions, volume 61, 1942, August section, pages 578–80.

25. New Formulas for Calculating Short-Circuit Stresses in Bus Supports for Rectangular Tubular Conductors, T. J. Higgins. *Journal of Applied Physics*, volume 14, March 1943, pages 151–4.

26. Die Stromkraft Bei Parallelen und Konzentrischen Leitern, W. Beetz. *Electrotechnick und Maschinenbau* (Vienna, Austria), volume 48, 1930, pages 761–3.

27. Formulas for the Calculation of the Inductance of Linear Conductors of Structural Shape, T. J. Higgins. AIEE Transactions, volume 62, 1943, pages 53–7.

28. Formulas for the Geometric Mean Distance of Rectangular Areas and of Line Segments, T. J. Higgins. *Journal of Applied Physics*, volume 14, 1943, pages 188–95.

Distribution Factors and Pitch Factors of the Harmonics of a Fractional-Slot Winding

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Synopsis: The investigation of the fractional-slot windings with respect to their harmonic (differential) leakage, to noise, and so forth, requires the knowledge of the distribution factors of their harmonics.

Very valuable work on distribution and pitch factors of fractional-slot windings has been done by J. F. Calvert.[1] On the basis of charts he worked out distribution-factor tables covering the actual harmonics up to the 24th. Once the chart of the winding has been set up, a simple formula can be used for computing the distribution factors of the harmonics for any three-phase fractional-slot winding.

In the following paper, simple formulas are derived for the distribution factors of the harmonics of fractional-slot windings which make the layout of charts unnecessary. These formulas are similar to those of the integral-slot windings. In deriving them the usual method of attack, namely, the use of charts, had to be abandoned and the vector diagram of the different slots used instead.

The Actual Harmonics of a Fractional-Slot Winding

IT has been shown in a previous paper[2] that

$$\left(\frac{n'}{p}+1\right)\frac{\beta}{2}=K \quad K \text{ integer excluding } 0 \quad (1a)$$

and

$$\left(\frac{n'}{p}\mp 1\right)\frac{\beta}{2m_1}=K_1 \quad K_1 \text{ integer including } 0 \quad (1b)$$

are the criteria for the existence of the n'th harmonic in the magnetomotive force of a fractional-slot winding. (For symbols see nomenclature.)

When the minus sign is used in the equation 1b, the harmonic travels with the rotation; when the plus sign is used, the harmonic travels opposite to the rotation.

Since n' has to satisfy both equations 1a and b, the actual harmonics produced by the winding will be different for different values of β and p. We have to distinguish between the case when β is equal to an even number and the case when β is equal to an odd number. It can be found easily that the actual harmonics of the three-phase windings are as follows:

When β = even number

$$n'=\frac{2p}{\beta}\nu \text{ where } \nu=1, 2, 3, 4, 5 \ldots \quad (2)$$

For the synchronous (main) wave $n'=p$ and, therefore, $\nu_s=\beta/2$. The number of subharmonics is equal to $(\beta-2)/2$.

When β = odd number

$$n'=\frac{p}{\beta}\nu \quad \text{where } \nu=1, 3, 5, 7 \ldots \quad (3)$$

For the synchronous wave here $\nu_s=\beta$ and the number of subharmonics is $(\beta-1)/2$.

The harmonics which are a multiple of three do not exist in the three-phase machine (except when zero-sequence currents flow in the winding).

The Slot Star

The use of the slot star instead of the generally used chart not only facilitates the layout of the fractional-slot winding but also makes it possible to derive simple formulas for the distribution factor and pitch factor of the harmonics of this winding.

Figure 1 represents the slot star of a winding with an integral number of slots per pole per phase, namely $q=2$. The integral-slot windings can be considered as a special case of the fractional-slot winding having $\beta=1$. Thus each pole represents a repeatable group (a unit) and only one pole has to be considered. The angle between two slots is

$$\alpha_s=\frac{180}{3\times 2}=30^\circ$$

To two adjacent vectors correspond two adjacent slots. The vector 7, with which the next pole starts, is shifted 180 degrees with respect to the vector 1. As regards the synchronous (main) wave, it is taken care of by a proper connection between the coil groups.

We consider now a winding with $1^1/_2$ slots per pole per phase, $q=1^1/_2$. If we write in general

$$q=\frac{N}{\beta} \quad (4)$$

β poles represent a repeatable group (a unit), each phase has N slots in β poles, and the total number of slots in β poles is equal to $3N$.

In our example two poles make a unit, each phase has three slots per unit, and the total number of slots per unit is equal to nine. The angle between two slots is as before.

$$\alpha_s=\frac{180}{mq} \quad (5)$$

and it is equal to 180/4.5=40 degrees. The angles which correspond to the nine slots of the unit are

Slot	1	2	3	4	5	6	7	8	9	10
Angle	0	40	80	120	160	200	240	280	320	360 = 0°

To slot 10 corresponds the angle zero degrees; slot 10 is the beginning of the next repeatable group. Slots 1 to 5 lie under the first pole, slots 6 to 9 lie under the second pole of the unit. Since the connections between the coil groups take into account a shifting of 180 degrees, the real angles between the slots, that is, the shifting of the slots with respect to each other in the magnetic field, are

Slot	1	2	3	4	5	6	7	8	9	10
Angle	0	40	80	120	160	20	60	100	140	180°

and the slot star of the repeatable group is given by Figure 2. Between vectors 1 and 2, which correspond to slots 1 and 2, lies the vector which corresponds to slot 6; between vectors 2 and 3, which correspond to slots 2 and 3, lies the vector which corresponds to slot 7, and so on. The winding creeps in the magnetic field. In general, between two vectors which correspond to two adjacent slots lie $(\beta-1)$ other vectors. We have to distinguish between the angle between

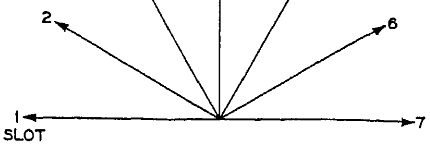

Figure 1. Slot star of a winding with $q=2$

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two slots as given by equation 5 and the angle between two adjacent vectors. This latter angle is the magnetic-field angle between the slots of the repeatable group; this angle determines the behavior of the winding with respect to its magnetomotive force and electromotive force, that is, its distribution factors and pitch factors.

The magnetic-field angle is

$$\alpha_m=\frac{180}{Nm} \tag{6}$$

$\alpha_s=\alpha_m$ only for the winding with an integral number of slots per pole per phase, because for this winding $\beta=1$ and $N=q$. For the fractional-slot windings α_m is always smaller than α_s. It is

$$\frac{\alpha_s}{\alpha_m}=\beta \tag{7}$$

Apparently, the highest distribution factor for the synchronous wave will be obtained when the first three vectors of the slot star, Figure 2, are assigned to phase I, the following three vectors are assigned to phase II, and the last three vectors to phase III. Thus, in each repeatable group phase I will occupy slots 1, 2, and 6; phase II, slots 3, 7, and 8; and phase III, slots 4, 5, and 9.

We consider the sequence of the slots in the slot star, Figure 2. If we start with slot 1, the sequence of the slots follows the series

$$1,\ 1+5,\ 1+2\times5,\ 1+3\times5\ \ldots\ldots$$

Since the total number of slots in the repeatable group is equal to $3\times N$, this value (or a multiple of it) has to be subtracted from the terms of the series when they are larger than $3N$.

In general, the series is

$$1,\ 1+d,\ 1+2d,\ 1+3d\ \ldots\ldots$$

where d is the difference between two slots which correspond to two adjacent vectors of the slot star.

This difference d can be found from the following consideration.[3] If we denote by P the number of full pole pitches between two slots which correspond to two adjacent vectors of the slot star (in our example $P=1$), then there will be

$$d\times\alpha_s=\alpha_m+180P$$

Inserting α_s and α_m from equations 5 and 6 there will be

$$d=\frac{m_1NP+1}{\beta} \tag{8}$$

For P the smallest integer for which d becomes an integer has to be inserted. P is equal to or larger than one.

The Distribution Factors of Three-Phase Windings

The slot star Figure 2 relates to the synchronous wave, the length of which is equal to 2τ. With respect to this wave the winding behaves as if there are N slots per pole per phase shifted with respect to each other by the magnetic-field angle α_m, which is equal to the angle between two adjacent vectors. Thus, the distribution factor of the synchronous wave is

$$K_d(n'=p)=\frac{\sin N^{\alpha_m}/2}{N\sin^{\alpha_m}/2}=\frac{\sin 30°}{N\sin 30°/N} \tag{9}$$

In order to determine the distribution factor of the n'th harmonic, it is only necessary to determine the angle between two adjacent vectors of Figure 2 for this harmonic. The number of slots per pole per phase N is the same for all harmonics.

We consider first the wave with the length equal to $2\beta\tau$. With respect to this wave the angle between two slots is,

$$\alpha_{s\beta}=\frac{\alpha_s}{\beta}$$

Since the winding behaves with respect to this wave as a normal integral-slot winding the magnetic-field angle must be equal to the slot angle, that is,

$$\alpha_{m\beta}=\alpha_{s\beta}=\frac{\alpha_s}{\beta}=\alpha_m$$

Figure 3 shows the slot star with respect to this wave. Adjacent vectors lie in adjacent slots. To two adjacent vectors of Figure 2 corresponds here the angle $d\alpha_m$. For example, to the two adjacent vectors 1 and 6 of Figure 2 corresponds here the angle $5\times\alpha_m=d\alpha_m$. Thus, for the harmonic the wave length of which is equal to $2\beta\tau$ the angle between two adjacent vectors of Figure 2 is equal to $d\alpha_m$. It is necessary to add to this angle 180 degrees when P is an odd number, in order to take into account the opposite direction of the current.

When we consider now the fundamental wave ($n'=1$), the wave length of which is equal to $2p\tau$, the angle between two adjacent vectors of Figure 2 for this wave will be $d\alpha_m\beta/p$ or $d\alpha_m\beta/p+180$ when P is an odd number. Therefore, the corresponding angles for the n'th harmonics are

$$\alpha_{mn'}=n'd\alpha_m\frac{\beta}{p}\quad\text{(when } P \text{ is an even number)} \tag{10}$$

$$\alpha_{mn'}=n'd\alpha_m\frac{\beta}{p}+180\quad\text{(when } P \text{ is an odd number and } \beta \text{ is even)}$$

$$\alpha_{mn'}=n'(d\alpha_m+180)\frac{\beta}{p}\quad\text{(when } P \text{ is an odd number and } \beta \text{ is odd)}$$

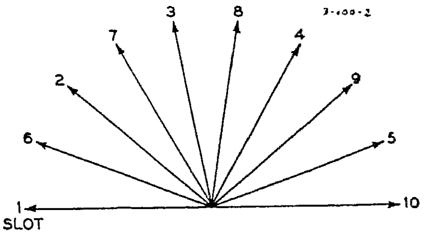

Figure 2. Slot star of a winding with $q=1^1/_2$ with respect to the synchronous wave

and the distribution factor of the n'th harmonic is

$$K_{dn'}=\frac{\sin N\,\alpha_{mn}'/2}{N\sin\alpha_{mn}'/2} \tag{11}$$

It can be shown that the numerator of equation 11 is equal to sin 30 degrees for all harmonics which are not a multiple of 3. We apply equations 2 and 3 to equation 10. We find:

When $\beta=$ even number

$$n'=\frac{2p}{\beta}\nu\qquad \nu=1,\ 2,\ 4,\ 5,\ 7\ \ldots\ldots\ \nu_s=\frac{\beta}{2}$$

and

$$\alpha_{mn'}=2d\alpha_m\nu+180 \tag{12}$$

P is here always an odd number: since N is here an odd number, d can become an integer only when P is an odd number.

When $\beta=$ odd number

$$n'=\frac{p}{\beta}\nu\qquad \nu=1,\ 5,\ 7,\ 11,\ 13\ \ldots\ldots\ \nu_s=\beta$$

$$\alpha_{mn'}=d\alpha_m\nu\quad\text{(when } P \text{ is an even number)}$$

$$\alpha_{mn'}=d\alpha_m\nu+180\quad\text{(when } P \text{ is an odd number)} \tag{13}$$

When β is an even number, N and P are odd numbers, d can be an odd as well as an even number, but d is not divisible by three. Thus, with N = odd number and

$$\alpha_m=\frac{180}{3N}$$

$$\sin N\,\alpha_{mn'}/2=\sin N(d\alpha_m+90)=\cos d\nu\times60°$$

The product $d\nu$ is not divisible by three, therefore

$$\sin N\alpha_{mn'}/2=\cos d\nu\times60=\cos 60°=\sin 30°$$

When β is an odd number, N, P, and d can be even as well as odd numbers; but d is not divisible by three; further, d is an odd number when N or P is an even number, and d is an even number when N and P are odd numbers. We have to consider the cases where P is even and those where P is odd.

When P is an even number, then

$$\sin N\alpha_{n'}/2=\sin Nd\alpha_m/2\times\nu=\sin d\nu\times30$$

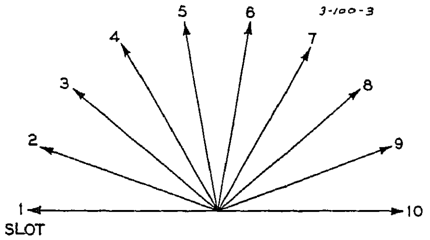

Figure 3. Slot star of a winding with $q=1^1/_2$ with respect to the wave whose length is equal to $2\beta\tau$

d and ν are odd numbers, not divisible by three. Thus $d\nu$ is an odd number not divisible by three and

$$\sin N\alpha_{mn'}/2 = \sin d\nu \times 30 = \sin 30 \text{ degrees}$$

When P is an odd number and N is an even number, then

$$\begin{aligned}\sin N\alpha_{mn'}/2 &= \sin N(d\alpha_m/2\times\nu+90)\\ &= \sin Nd\alpha_m/2\times\nu = \sin d\nu\times 30\end{aligned}$$

and the result is the same as for P equal to an even number.

When P is an odd number and N is an odd number, then d is an even number, and there will be

$$\begin{aligned}\sin N\alpha_{mn'}/2 &= \sin N(d\alpha_m/2\times\nu+90)\\ &= \sin(d\nu\times 30+90) = \cos d\nu\times 30\end{aligned}$$

Since $d\nu$ is an even number

$$\sin N\alpha_{mn'}/2 = \cos d\nu\times 30 = \sin 30^\circ$$

Therefore, we can write for equation 11

$$K_{dn'} = \frac{\sin 30^\circ}{N\sin\alpha_n'/2} \tag{14}$$

$\alpha_{n'}$ is given by equations 12 and 13. Inserting these equations and also equations 6 and 8, we find finally

When β = even number

$$K_{dn'} = \frac{0.5}{N\cos\left(\frac{d}{N}60\nu\right)} \tag{15}$$

When β = odd number

$$K_{dn'} = \frac{0.5}{N\sin\left(\frac{d}{N}30\nu\right)} \text{(when } P \text{ is an even number)} \tag{16}$$

$$K_{dn'} = \frac{0.5}{N\cos\left(\frac{d}{N}30\nu\right)} \text{(when } P \text{ is an odd number)} \tag{17}$$

Equations 15 to 17 are valid for the absolute values only. Equations 10 and 11 yield also the right signs.

Examples

Three-phase machine with 14 poles and $q=11/7$. Thus, $N=11$, $\beta=7$. Equation 8 yields $d=19$ with $P=4$. Since β is an odd number and P an even number, equation 16 has to be applied. It is $p=\beta$ and, therefore, $n'=\nu$ (see equation 3).

It is valid

$$K_{dn'} = \frac{0.5}{11\sin\frac{19}{11}30} = 0.058 \text{ for the fundamental wave}$$

$$K_{dn'} = \frac{0.5}{11\sin\frac{19}{11}30\times 5} = 0.0463 \text{ for the fifth harmonic}$$

and so forth.

Eight-pole machine with $q=11/4$, $N=11$, $\beta=4$, $d=25$ with $P=3$.

For $2p/\beta=2$, $n'=2\nu$ (see equation 2). Thus,

$$K_{dn'} = \frac{0.5}{11\cos\frac{25}{11}60} = 0.0625 \text{ for the second harmonic}$$

$$K_{dn'} = \frac{0.5}{11\cos\frac{25}{11}60\times 4} = 0.0457 \text{ for the eighth harmonic}$$

and so forth.

The Pitch Factors of Three-Phase Windings

For the pitch factor of the synchronous wave

$$K_{p(n'=p)} = \sin\frac{W}{\tau}90 \tag{18}$$

Denoting by Z the coil width in slot pitches, we can write for the synchronous wave

$$\begin{aligned}K_{p(n'=p)} &= \sin\frac{Z}{mq}90 = \sin\frac{Z\beta}{mq\beta}90\\ &= \sin\frac{Z\beta}{mN}90 = \sin Z\beta\frac{\alpha_m}{2}\end{aligned} \tag{18a}$$

Referring α_m as in section 3 to the wave the length of which is equal to the total circumference of the armature ($2p\tau$), we find for the pitch factor of the n'th harmonic

$$K_{pn'} = \sin Z\beta\frac{\alpha_{n'}}{2} \tag{19}$$

$\alpha_{n'}$ is given by equations 12 and 13. As for the distribution factor, it must be distinguished between the case when β is an even number and the case when β is an odd number. Inserting in equation 19 equations 12, 8, and 6 for β = even number, and equations 13, 8, and 6 for β = odd number, we find in agreement with Calvert

$$K_{pn'} = \sin\frac{W}{\tau}\frac{180}{\beta}\nu \quad \text{(when } \beta = \text{even number)} \tag{20}$$

$$K_{pn'} = \sin\frac{W}{\tau}\frac{180}{2\beta}\nu \quad \text{(when } \beta = \text{odd number)} \tag{21}$$

Nomenclature

d—difference between two slots which correspond to two adjacent vectors of the slot star.

$K_{dn'}$—distribution factor of the n'th harmonic.

$K_{pn'}$—pitch factor of the n'th harmonic.

m_1—number of phases.

n'—order of the harmonic with respect to a fundamental whose wave length is equal to the circumference of the armature.

N—numerator of the fraction that fixes the number of slots per pole per phase.

p—number of pole pairs.

P—number of full pole pitches between two slots which correspond to two adjacent vectors of the slot star.

$q=\frac{N}{\beta}$—number of slots per pole per phase.

W—coil width.

Z—coil width in slot pitches.

$\alpha_s=\frac{180}{mq}$—angle between two slots.

$\alpha_m=\frac{180}{mN}$—angle between two vectors of the slot star.

β—denominator of the fraction that fixes the number of slots per pole per phase.

τ—pole pitch (in the units of W).

References

1. Amplitudes of Magnetomotive-Force Harmonics for Fractional-Slot Windings of Three-Phase Machines, J. F. Calvert. Iowa Engineering Experiment Station *Bulletin* 142, 1939.

2. Field Harmonics in Induction Motors, M. M. Liwschitz. AIEE Transactions, volume 61, 1942, November section, pages 797–803.

3. Le Macchine Elettriche (3rd volume), M. M. Liwschitz. Hoepli, Milano, Italy, 1937.

See also bibliography to references 1 and 2.

Operation of Nonsalient-Pole-Type Generators Supplying a Rectifier Load

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IT has been recognized that the presence of harmonic currents in the supply circuit are a source of additional heating in the generators supplying a rectifier load, particularly where a six-phase rectifier is the total load. Robert Pohl has presented papers on this subject, and from his work has concluded that an appreciable reduction in the rating of turbine generators is required when the rectifiers are connected for 6-phase and 12-phase operation.[1,2] Experience in this country with turbine generators operating with rectifier loads indicated that the reduction factors presented by Doctor Pohl were too large, so a test program was carried out to determine the practical limits of such loads. The test results and experience both indicate that for 12-phase operation and above, the effect on the supply system is very small and in most cases may be neglected.

Any harmonic current flowing in the stator winding of a generator increases the temperature rise of the stator winding and surrounding parts. Also, these harmonics establish a magnetomotive force which results in currents flowing in the surface of the rotor, adding to the rotor heating. It can be shown that some harmonics rotate backward, and some harmonics rotate forward with respect to the fundamental.[3] Because it rotates against the rotation of the rotor, the magnetomotive force set up by the fifth harmonic results in a sixth harmonic on the surface of the rotor. Since the rotation of the seventh harmonic is in the same direction as that of the rotor, it also results in a sixth harmonic on the rotor surface. These two magnetomotive forces, rotating in opposite directions, result in a pulsating field on some axis of the rotor. Likewise, another pulsating field of 12 times normal frequency is set up by the action of the 11th and 13th harmonics. The harmonics, up to the 25th, present in the stator and rotor are shown in Table I.

Tests

The temperature tests were made on a 1,250-kva 2,300-volt three-phase, 60-cycle 3,600-rpm turbine generator supplying a 600-volt ignitron rectifier connected for double three-phase operation, as shown in Figure 2. Double three-phase is a form of six-phase operation. The regulation of the rectifier was approximately ten per cent. That is, the impedance of the lines, transformer, and rectifier was of such a value that there was a drop in voltage equal to ten per cent of the normal voltage when full generator load was applied. In addition, the lines were not supplied from what might be considered an infinite system, but instead, from a single generator. The subtransient reactance of this generator was approximately 11 per cent. The importance of this fact and its effect on the magnitude of harmonic currents will be shown later. The generator was operated at approximately full kilovolt-amperes for four temperature runs, with the temperature of the stator winding being measured by detector, and the temperature of the rotor winding being measured by the increase in resistance. Oscillograms of the wave form were taken during each run, in order to obtain the magnitude of the harmonic currents to be used in subsequent calculations. After each test, the rotor was inspected to determine whether burning had occurred, either between the wedges and the teeth, or at the joints between the retaining rings and the rotor body. There was, however, no evidence of burning on any of the runs, indicating that excessive heating did not occur under the loading conditions of the test. Table II shows the results of the tests. In order to obtain the additional temperature rise of the stator and rotor windings, preliminary temperature runs were made at zero load and full voltage; full kilovolt-amperes and 90 per cent power factor; and full kilovolt-amperes and zero per cent power factor. Using the results of these tests, it was possible to determine what the temperatures would have been if the harmonic currents had not been included in the load current. The additional temperature rise shown in the tables is the difference between the measured temperature rise and the normal temperature rise for that particular load.

Rotor Heating

In order to calculate the heating of the rotor, the loss can be determined by the use of the negative-sequence resistance of

Table I. Harmonics Present in Stator and Rotor

	Stator Harmonics		Resultant Rotor Harmonics		
Order of Harmonic	6-Phase Connection	12-Phase Connection	6-Phase Connection	12-Phase Connection	Rotation of Harmonics
1	1	1			Forward
3					
5	5		6		Backward
7	7		6		Forward
9					
11	11	11	12	12	Backward
13	13	13	12	12	Forward
15					
17	17		18		Backward
19	19		18		Forward
21					
23	23	23	24	24	Backward
25	25	25	24	24	Forward

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Table IIA. Generator Test Results—Temperature Runs

Rectifier Connected for Double Three-Phase Operation

Phase Delay (Degrees)	Voltage	Current	Power Factor	Stator Temperature Rise (Degrees Centigrade)		Rotor Temperature Rise (Degrees Centigrade)		Ambient Temperature (Degrees Centigrade)
				Total	Additional	Total	Additional	
0	2300	297	90	40	2.6	79.1	19.1	28.8
14.1	2300	302	89.3	40.8	2.8	86	22	29.7
24.7	2300	296	83.5	42.5	5.1	93.1	22.1	25.4
30.4	2300	295	81.5	44	6.6	95.5	25.5	28.8

Table IIB. Generator Test Results—Per Cent Harmonic Currents in Supply Circuit

Rectifier Connected for Double Three-Phase Operation

Order of Harmonic	Phase Delay (Degrees)			
	0	14.1	24.7	30.4
1	99.0	99.5	98.26	98.3
3	0.10	1.76	0.99	1.80
5	11.80	13.50	15.30	15.30
7	6.72	7.24	5.60	6.63
9	0.11	1.29	0.48	2.05
11	2.41	4.10	5.77	5.38
13	1.75	2.92	3.05	3.89
17	1.03	1.10	2.94	2.61
19	1.18	0.53	1.59	2.28
23	0.33	1.17	1.59	1.15
25	0.76	0.89	0.86	1.02

the rotor. A description of the test method for obtaining this value is given in the appendix. A minor correction in the value of resistance must be made, since the loss varies as the 1.8 power of the negative-sequence current for turbine generators.[4] Since the fifth and seventh harmonics result in two sixth-harmonic waves rotating in opposite directions on the surface of the rotor, the current to be used in calculating the sixth-harmonic loss can be taken as the sum of the fifth and seventh harmonics. This figure of loss must be multiplied by three, for the three phases, and by two, because at 120 cycles only one-half the rotor loss is $I_2^2R_2$, the other half being supplied through the shaft. The sum of the harmonic currents gives the maximum loss for the complete periphery of the rotor, but the actual loss is an average between the points of maximum and minimum loss. Thus, the maximum loss must be reduced by a factor, K_a, in order to obtain the average loss. The factor K_a may be obtained from Figure 5, once the ratio of the two harmonic currents is known. The derivation of the curve in Figure 5 is given in the appendix.

In obtaining the temperature rise, a factor, K_v, of 0.11 watt per degree centigrade per square inch of rotor surface, neglecting the surface of the retaining rings, was used for this particular peripheral speed. An air velocity of one-half the peripheral speed of the rotor was assumed. Since the temperature rise was measured on the rotor winding instead of the rotor surface, it was necessary to take into account the portion of the rotor winding which was affected by the additional loss on the rotor surface. The construction of the rotor for this machine was such that the ventilation of the portion of the coils beneath the retaining rings was very good, and it was assumed that all the extra heat in these sections of the winding was dissipated to the air passing over the end turns of the rotor winding, leaving only the slot portion of the winding to be affected by the additional loss on the surface of the rotor. On this rotor, the slot portion of the winding was 54 per cent of the mean turn, and this value was used for the winding ventilation factor, K_w. This factor may vary widely, depending on the physical proportions of the rotor, as well as the amount of ventilation on the end turns of the rotor winding.

The temperature rise of the rotor winding caused by the harmonic currents may be calculated for the various even-numbered harmonics from the following equation, using the currents of order $n-1$ and $n+1$, where n is the order of the harmonic on the surface of the rotor.

$$\text{Temperature rise} = 2\times 3\times \frac{K_w}{K_v\times S}\times \Sigma K_a(I_{n-1}+I_{n+1})^2(R_2-R_1)\sqrt{\frac{f}{120}}$$

where

K_a = average loss factor
K_w = winding ventilation factor
K_v = heat-dissipation factor
S = rotor surface ($\pi\times$rotor diameter$\times$ core length)

Table III compares the calculated and test values for the four temperature runs.

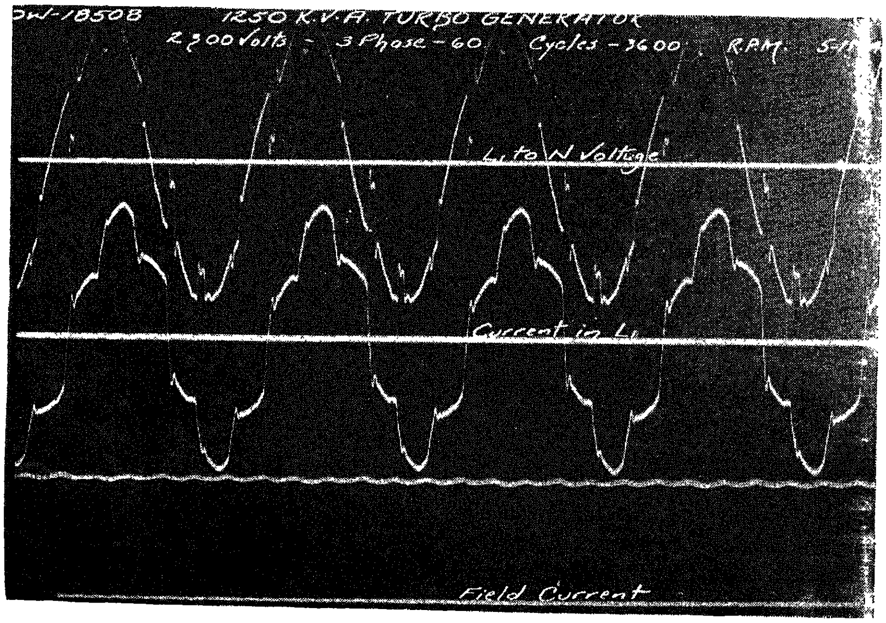

Figure 1. Oscillogram showing wave form in generator field and armature circuits

Double three-phase ignitron operating with 30.4 degrees phase delay. 1,250-kva turbogenerator, 2,300 volts, three phase, 60 cycles, 3,600 rpm

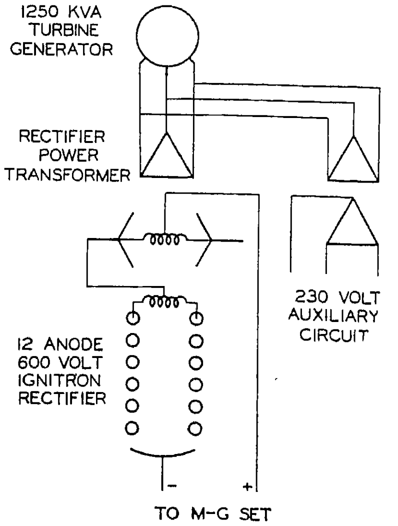
1250 KVA TURBINE GENERATOR
RECTIFIER POWER TRANSFORMER
230 VOLT AUXILIARY CIRCUIT
12 ANODE 600 VOLT IGNITRON RECTIFIER
-
+
TO M-G SET

Figure 2. Schematic diagram f test circuit

Table III. Additional Temperature Rise of Rotor Winding

Reduction Factors Applying to This Machine Connected to Double Three-Phase Ignitron

Phase Delay (Degrees)	Additional Rise (Degrees Centigrade)		Load Reduction Factor	
	Test	Calculated	Test	Calculated
0	19.1	19.3	1.0	1.0
14.1	22.	27.6	0.995	0.95
24.7	22.1	32.3	0.92	0.85
30 4	25.5	32.2	0.9	0.85

The reduction in load to maintain not more than 85 degrees centigrade rotor temperature rise was calculated for each load condition and is given in Table III as a reduction factor. To determine this value, the additional temperature rise of the rotor caused by the harmonic currents was varied as the square of the load current, and the rise caused by the field current was calculated from the saturation curves of the machine. The two components were added by trial until the proper figure was obtained. It should be noted that these reduction factors apply only to this particular machine, and they cannot be generalized to cover all machines.

Stator Heating

The calculation of the heating of the stator winding is complicated further by the fact that the temperatures by detector are affected not only by the I^2R loss as determined by the eddy factor, but also by the heating of the stator parts, and by the extra loss on the surface of the rotor. It has been suggested that the load on the stator should be reduced according to the square root of the ratio of the normal loss to the loss with rectifier load as calculated by the use of the eddy factor alone, and neglecting any effect of core loss or rotor loss.[1] This method has been followed in the calculations for Table IV. These calculations have been made for the average loss, using the average eddy factor and for the maximum loss in the top strand. The effective eddy factor, $K_{\text{eff.}}$, equals $\Sigma I^2K_e/\Sigma I^2$, and the reduction, R, in load equals $\sqrt{K_e}$ (fundamental)$/K_{\text{eff.}}$ The reduction factors, R and R', for the average and top strands, respectively, may be compared with the reduction factors from the measured additional temperature rise by inspection of Table IV.

The stator temperature rise on the normal full-load test was 38 degrees centigrade, which is considerably below the maximum value of 60 degrees centigrade rise. While this stator had considerable temperature margin, it was thought worth while to prepare a series of load reduction factors based on maintaining the full-load temperature rise of 38 degrees centigrade with various rectifier loads. It was assumed that the permissible load varied as the square root of the ratio of the temperature rise under normal load to the temperature rise with rectifier load. The reduction factors for this machine alone are given in Table IV.

Table IV. Additional Temperature Rise of Stator Winding

Reduction Factors Applying to This Machine Connected to Double Three-Phase Ignitron

Phase Delay (Degrees)	Additional Rise From Test (Degrees Centigrade)	Reduction Factor From Test	Calculated Reduction Factor	
			Average	Top Strand
0	2.6	0.966	0.963	0.95
14.1	2.8	0.965	0.94	0.917
24.7	5.1	0.937	0.915	0.895
30.4	6.6	0.922	0.907	0.887

12-Phase Operation

The foregoing tests and calculations were all on the basis of the rectifier operating with a double three-phase connection. If the connection had been 12 phase, the magnitude of the 5th, 7th, 17th, and 19th harmonics would have been reduced, with some improvement in power factor. In order to compare the performance with double 3-phase and 12-phase connections, the calculations have been made, eliminating the 5th,

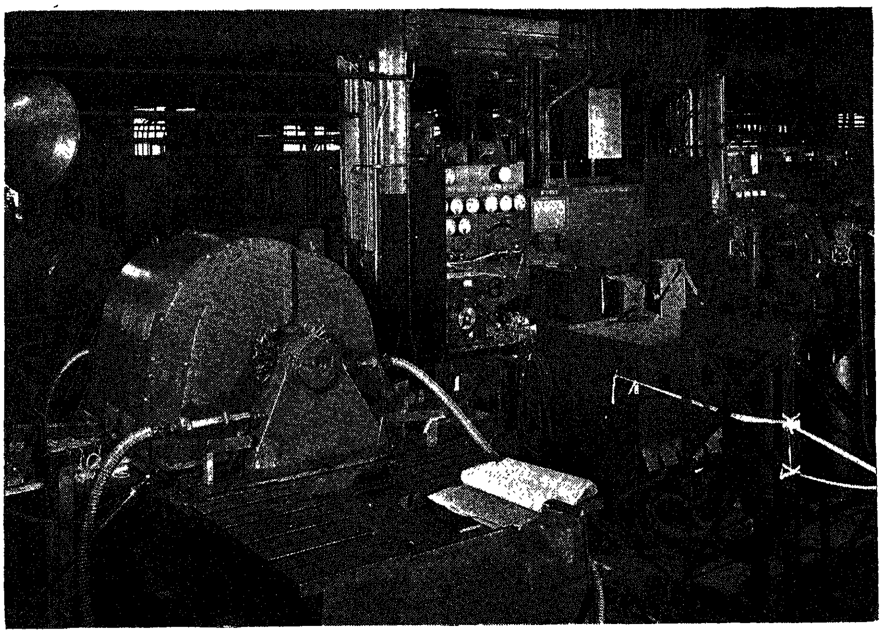

Figure 3 (above). 1,250-kva turbine generator on test

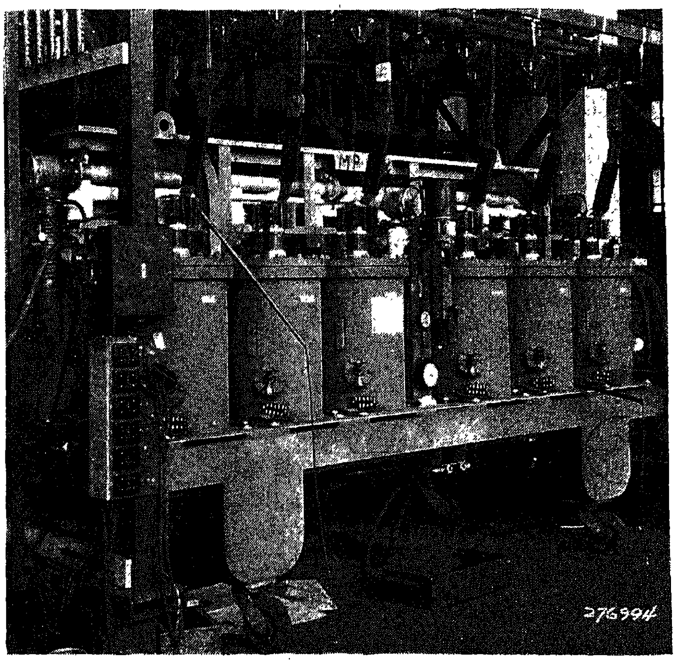

Figure 4 (right). 12-anode ignitron on test

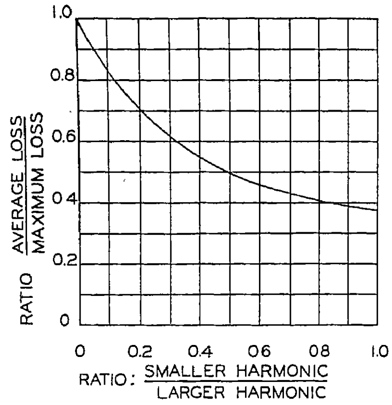

Figure 5. Values of rotor loss factor K_a

7th, 17th, and 19th harmonics, and assuming the magnitudes of the others were unchanged. The calculated reduction factor for the stator winding was 0.96 compared with 0.907 for the condition of 30.4 degrees phase delay. The calculated additional temperature rise for the rotor winding was approximately six degrees for the 30.4-degree delay condition compared to 25.5 degrees for the double three-phase connection Considering the improvement in power factor and the lower temperature rise, the rotor heating for the assumed conditions will not affect the permissible loading.

Conclusions

From these tests and calculations, and from previous studies of rectifier operation, the following conclusions may be drawn:

1. A rectifier load is the source of harmonic currents in the supply system. These harmonic currents cause extra heating in the stator and rotor of a generator, and, in some cases, may require a reduction in rating of supplying generators.

2. The order of the harmonic currents is affected by the number of phases of rectifier operation, and their magnitude is affected by the voltage reduction or phase delay, and the impedance of the complete system.[5,6,7] The lowest-order harmonic current which exists in the supply circuit is $P-1$, where P is the number of phases of rectifier operation. With a rectifier operating with a 12-phase connection, the additional heating of the generator will be slight, and with a number of phases higher than 12, the additional heating will be negligible. The magnitude of the harmonic currents is reduced with a reduction in degrees ignitor delay and an increase in system impedance.

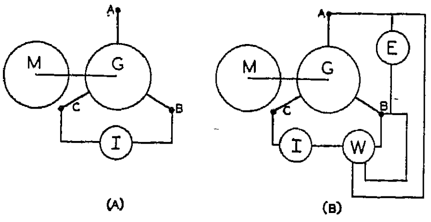

Figure 6. Generator connections for obtaining negative-sequence resistance

3. For a given load, the rotor heating is more pronounced than the stator heating, but operation at a relatively high power factor will introduce some margin in the rotor heating. Under certain conditions of operation the stator heating becomes the limiting factor.

4. It is not possible to state any definite rule for the reduction in rectifier load necessary in order to keep generator temperatures within recognized limits, because the extra heating is affected by the physical proportions of the machine, as well as the loading conditions. It is possible, however, to determine the magnitude of the harmonic currents,[5,6] once the loading conditions are known. The extra heating and any necessary reduction in load can be determined by the generator designer, using the methods outlined in this discussion.

Appendix A

Method of Obtaining Negative-Sequence Resistance

The negative-sequence resistance may be determined by either one of two methods.[4,8]

For Figure 6a, drive the generator at synchronous speed.

$$R_2=\frac{3(P-P_{F\&W})}{2I^2}$$

where

P = shaft input in watts
$P_{F\&W}$ = friction and windage loss in watts
I = phase current

For Figure 6b, drive the generator at synchronous speed.

$$\text{Power factor}=\frac{P}{E\times I}=\cos\theta$$

$$Z_2=3\frac{E}{I}$$

$$R_2=\frac{E}{3}\frac{\sin}{I}\theta$$

where

P = power in watts
E = line-to-line voltage
I = phase current

It should be noted that both of these methods require a sustained, single-phase short circuit, and care should be taken to see that the rotor is not overheated.

Appendix B

Since the fifth harmonic current rotates against the direction of rotation of the rotor, it generates a sixth harmonic current in the rotor surface. The seventh harmonic current rotates with the rotor, also generating a sixth harmonic current in the rotor surface, but the direction of rotation is opposite to that caused by the fifth harmonic current. Thus, there are two points of maximum loss and two points of minimum loss, with a sinusoidal distribution between. This relationship holds for any pair of harmonic currents which generate any particular frequency in the rotor surface. The distribution is shown in Figure 7, where A is the smaller of the two harmonic currents and B is the larger.

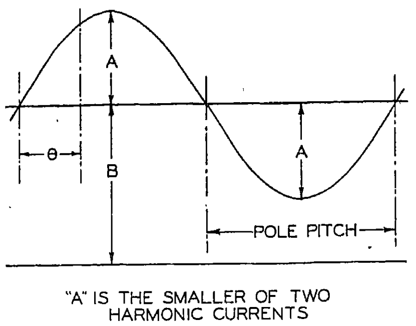

Figure 7. Resultant current distribution around rotor periphery

Let average loss around periphery equal L and angle around periphery equal θ, then

$$L=\frac{1}{2\pi}\int_0^{2\pi}(B+A\sin\theta)^2d\theta$$
$$=\frac{1}{2\pi}\left[B^2\theta-2AB\cos\theta+A^2\left(\frac{\theta}{2}-\frac{\sin 2\theta}{4}\right)\right]_0^{2\pi}$$
$$=B^2+\frac{A^2}{2}$$

Figure 5 gives the ratio of the average loss to the maximum loss for any ratio of the two harmonic currents.

References

1. OUTPUT LIMITS OF TURBO-ALTERNATORS ON RECTIFIER LOAD, Robert Pohl. *Elektrotechnik und Maschinenbau* (Vienna, Austria), April 28, 1935, pages 193–6.

2. HEATING OF TURBO-ALTERNATOR ROTORS DUE TO RECTIFIER LOAD, Robert Pohl. *The Engineer*, September 26, 1941, pages 204–05.

3. ALTERNATING-CURRENT MACHINES (book), Puchstein and Lloyd. John Wiley and Sons, Inc. Page 249.

4. ELECTRICAL TRANSMISSION AND DISTRIBUTION REFERENCE BOOK. Westinghouse Central Station Engineers. Page 145.

5. CURRENT AND VOLTAGE WAVE SHAPE OF MERCURY-ARC RECTIFIERS, H. D. Brown, J. J Smith. AIEE TRANSACTIONS, volume 52, 1933, pages 973–86.

6. GRID-CONTROLLED RECTIFIERS AND INVERTERS, C. C. Herskind. AIEE TRANSACTIONS, volume 53, 1934, June section, pages 926–35.

7. HARMONICS IN THE A-C CIRCUITS OF GRID-CONTROLLED RECTIFIERS, R. D. Evans, H. N. Muller, Jr. AIEE TRANSACTIONS, volume 58, 1939, pages 861–8.

8. PRELIMINARY REPORT—TEST CODE FOR SYNCHRONOUS MACHINES. AIEE, January 1937. Page 26.

9. REGULATION OF GRID-CONTROLLED RECTIFIERS, L. A. Kilgore, J. H. Cox. AIEE TRANSACTIONS, volume 56, 1937, September section, pages 1134–40.

10. HARMONICS AND LOAD BALANCE OF MULTIPHASE RECTIFIERS, R. D. Evans, AIEE TRANSACTIONS, volume 53, 1934, April section, pages 182–7.

11. HEAT FLOW IN TURBINE GENERATOR ROTORS, C. E. Peck. AIEE TRANSACTIONS, volume 53, 1934, October section, pages 1359–65.

Rotating Regulator for Arc Furnaces

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Synopsis: Increased production of steel alloy for the war effort has brought about a wider use of electric-arc furnaces, and with this improvements have been made in automatic regulation of electrode position.

The Rototrol regulator described in this paper is of the rotating type which controls electrode position as determined by response to current in the electrode and voltage between the electrode and the furnace shell. Features and performance of a typical installation are illustrated.

AUTOMATIC adjustment of electrode position has always been used as a means of controlling the power input to steel-melting arc furnaces. This is necessary because the charge melts and flows away from the electrode so that the electrode must be lowered to maintain the arc. The scrap being melted frequently falls against the electrode, making withdrawal necessary to remove the short circuit. Furthermore, adjustment is necessary to allow for natural electrode consumption.

In addition to this the arc is a pure resistance load, and changing its length by adjustment of the electrode position gives a simple and satisfactory method of controlling the current.

The early arc-furnace regulator was a simple current-operated device. The electrode current in each phase was supplied from a current transformer to the coil of the regulator element. The pull of this coil was then balanced against a spring, and the operation of the regulator contacts caused the hoist motor to run in either direction to raise or lower the electrode as the current varied from some predetermined value. Figure 1 shows a typical arc furnace for steel melting. The hoist motors as shown are connected through a wire cable and drum sheave so as to lower and raise the electrode masts from which the electrodes are supported. This mechanical system is varied somewhat with different designs of furnace, but the electrical problem is simply one of automatic control, suitable for reversing and braking the electrode motor.

Current regulation was generally unsatisfactory when precise control was necessary, chiefly because the restraining force of the spring had a constant value. The variations of the current in the circuit are rapid at times, and the mechanical movement of the electrode is slow. Therefore, the position of the electrode responded not to instantaneous but to an average value of current variation. Furthermore, a three-phase system of currents in an electric furnace is complex. The furnace charge is a ground which connects through three continually shifting phase arcs to the potential supplied by the transformer. It is clear that the currents in the regulator elements are not independent but rather are interdependent; the value in any phase at a given time is determined by conditions in the other two phases, as well as by the length of the arc in that particular phase.

As is well known, the voltage drop across the arc will increase as the current is decreased and vice versa. In practice this characteristic can be modified by inserting additional reactance in the circuit. In fact, a sufficient value of reactance must be present in the circuit to maintain stability, so that the combination of arc resistance and circuit impedance will have a positive impedance characteristic over the operating zone. These theoretical requirements of the arc-furnace circuit have been frequently discussed in other articles on the subject and are well known. It should not be forgotten, however, that a considerable part of the circuit reactance in the case of the larger furnaces is in the secondary leads, and, therefore, such furnaces require no additional reactance for stable operation.

It is readily understood that, when the varying pull of the coil or other regulating device which is proportional to current in the electrode, is matched against a restraining force that is proportional to the voltage drop across the arc, the utmost sensitivity to changes in arc length is obtained in the regulator. While this sensitivity may not be so important when the furnace is reducing the cold scrap to a molten bath, it is of great importance when this molten bath is being refined.

The development of the art over a period of years showed that this principle of regulation was correct. It was natural that further development should look for improvements of the controlling scheme for the motor which for many years had been a constant-potential d-c, reversing control combined with dynamic braking.

Quite independently of the arc-furnace industry, however, new methods of regulation had been introduced in other industrial applications where regulated control of motors to adjust for speed, tension, rate of acceleration, and so forth, were desired. These methods for the

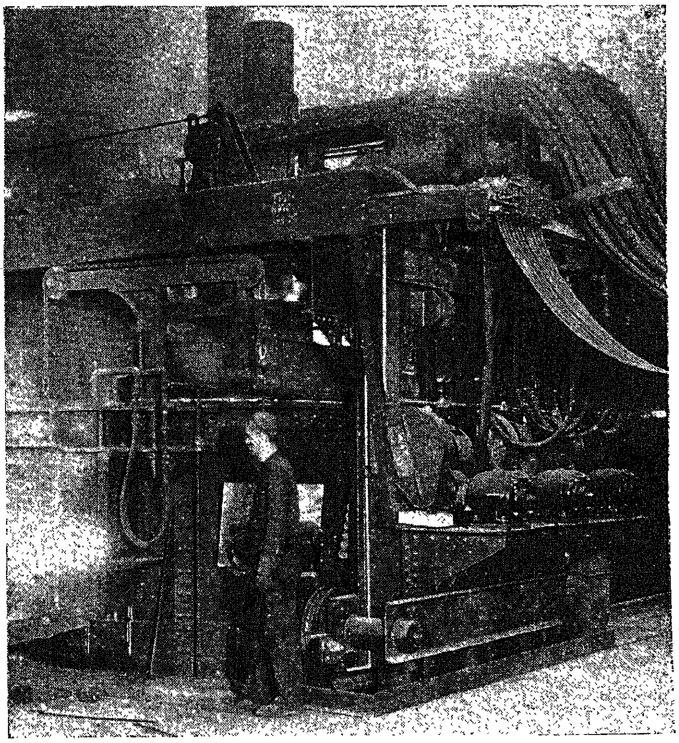

Figure 1. Representative arc furnace for steel melting

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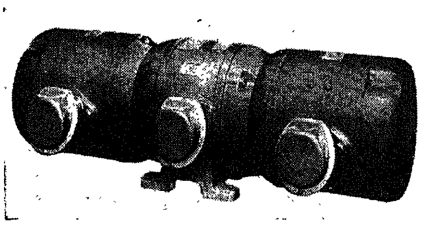
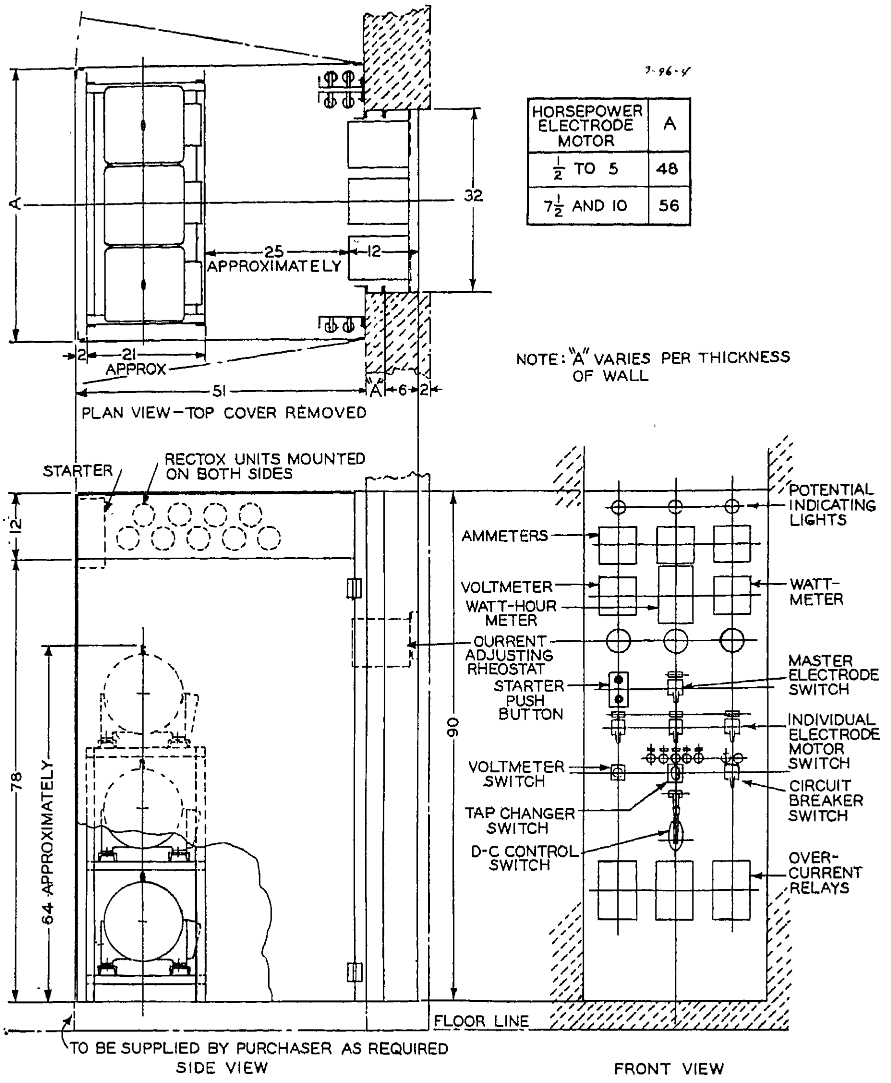


Figure 2. Typical unit including Rototrol, a-c motor, and d-c generator

most part were based on a different means of operating the motor in response to the regulating element. Instead of using a constant-potential control, adjustable-voltage control of the motors was used and has been found very satisfactory. An independent source of d-c voltage is required and is usually supplied by a motor generator set. If the exciter for this generator is supplied with suitable control fields, a complete regulating system is formed in which the regulating element is a part of the power supply for the electrode motors.

The Rototrol regulator was first applied to an automatic welding head in 1928. A few years later it was applied to the large elevators in Radio City and, since then, has found wide use in many industrial applications. It is the heart of the adjustable-voltage planer drive and has been proved in service over a period of several years. Other applications include paper-mill drives, electric shovels, mine sweepers, skip hoists, bomb spinners, and machine tools, some of which provide speed ranges as high as 120 to 1.

The rotating-type arc-furnace regulator was thus a natural development prompted by the use of similar regulators in other applications. It has been favorably received chiefly because the direct tie between the generator and the motor eliminates all contactors in the motor circuits.



HORSEPOWER ELECTRODE MOTOR	A
½ TO 5	48
7½ AND 10	56

Figure 4. Metal-enclosed Rototrol arc-furnace control and regulator unit

The Rototrol is essentially a small d-c machine similar in both construction and theory of operation to the standard d-c generator of like size. It is driven by a standard squirrel-cage induction motor as part of the motor generator set, Figure 2. The magnetic circuit is excited by a number of field windings, and the machine functions entirely through the interaction of these fields. The field coils constitute the input of the device, while the output is obtained from the armature circuit. The principal field windings include:

1. A control field which serves as a reference against which the quantity to be regulated is compared.
2. A control field which measures the quantity to be regulated.
3. A self-energizing field connected either in series or in shunt with the armature.

For a high degree of accuracy, the various control fields should have complete control over the regulating element without supplying any of the magnetomotive force necessary to generate power in its own output circuit. The self-energizing field circuit is designed so that its resistance line will be coincident with the air-gap line of the no-load saturation curve. This results in a high degree of amplification since an extremely small change of power in the control fields is ample to change the output over its full range.

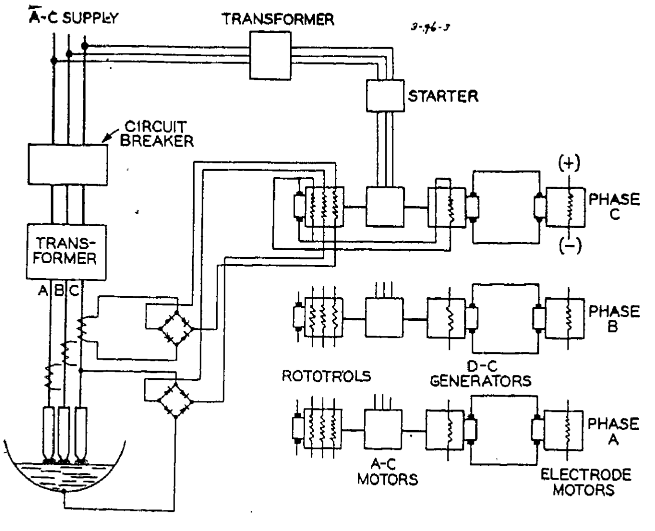


Figure 3. Elementary diagram of arc-furnace regulating system

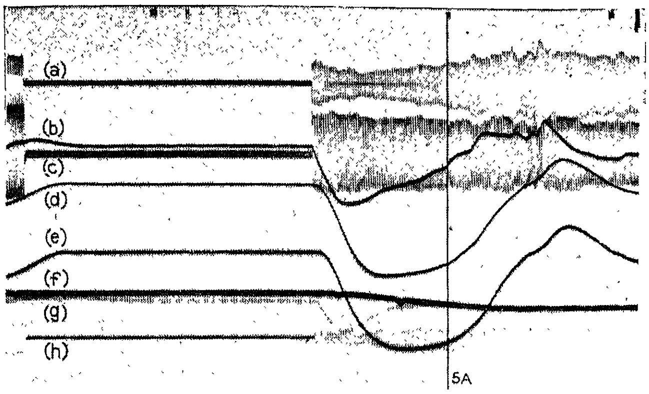


Figure 5. Oscillogram showing electrical performance of regulator during the melt-down period

(a). Electrode current to Rototrol control field
(b). Rototrol armature volts
(c). A-c volts to Rototrol control field
(d). Motor armature volts
(e). Motor speed
(f). Electrode position indicator
(g). Motor armature current
(h). Electrode current

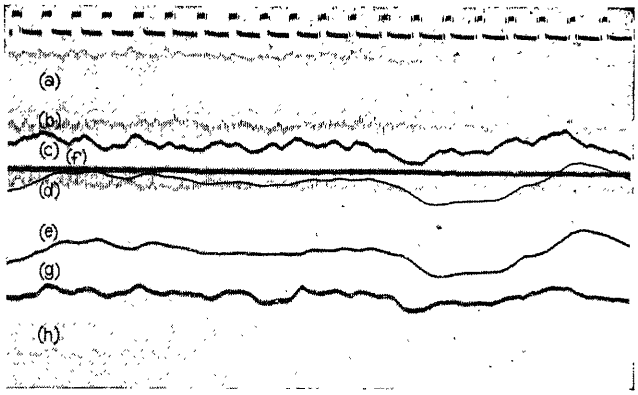


Figure 6. Oscillogram showing electrical performance of regulator during the refining period

(a). Electrode current to Rototrol control field
(b). Rototrol armature volts
(c). A-c volts to Rototrol control field
(d). Motor armature volts
(e). Motor speed
(f). Electrode position indicator
(g). Motor armature current
(h). Electrode current

This output is applied to the field of the constant-speed direct-connected generator, which supplies power to the electrode motors.

The speed of response of the system comprising the arc-furnace regulating element and the power-supplying generator must be carefully co-ordinated with the acceleration characteristics of the electrode motor in order to obtain the maximum rate of acceleration permissible without exceeding the commutating ability of the electrode motor. Acceleration of the electrode motor is influenced by the system inertia, the friction load, and the torque developed by the motor. For instance, a three-horsepower equipment with a system WR^2 at the motor shaft of 1.5 pound-feet squared, a friction load of two-thirds full-load torque and a maximum motor torque of 1.5 times rated will accelerate to rated speed in about one half of a second.

The speed of response of the regulating element depends on the ratio of inductance to resistance for each field, the mutual induction between the fields, and the relative ratios of turns and time constants of the various fields. It is particularly important that time-constant and relative-turn ratio of the self-energizing field be correctly proportioned. Assuming a three-field machine, the differential equation of load voltage e will be:

$$\begin{aligned} e &= k_2i_2-k_1i_1+k_3i_3 \\ &= i_3r_3+L_3\frac{di_3}{dt}+M_{23}\frac{di_2}{dt}+M_{13}\frac{di_1}{dt} \end{aligned}$$

where

k_1, k_2, k_3 are machine constants with various field turns.
i_1, i_2, i_3 are the respective field currents.
r_3 is the self-energizing field resistance.
L_3 is the self-energizing field inductance.
N_{23}, M_{13} are the mutual inductances between fields 2 and 3, and between fields 1 and 3.

By combining this with the equations for other field voltages, the differential equation of voltage build-up in the output circuit can be obtained. Solution of equations of this type are of the forms:

$$e=A_1\epsilon m_1t+A_2\epsilon m_2t+A_3\epsilon m_3t+B$$

where the A's, m's, and B are constants depending on L, R, and M.

By proper proportion of the field circuits, any reasonable speed of response as required for electrode control can be obtained. Optimum performance is obtained when the speed of response of the electrical system is co-ordinated with acceleration characteristics of the mechanical system.

Figure 3 shows an elementary diagram of the regulating system. The current control field of the regulating element is energized from a dry-type rectifier which is in turn connected to a current transformer in the electrode circuit. The potential control field is energized in a similar manner from the voltage between each electrode and the shell of the furnace. In practice, when the breaker in closed voltage is applied to the potential control field which causes the generator voltage to build up in the direction to lower the electrodes. When the first electrode strikes the metal, the voltage drops to zero causing the motor to stop. When a second electrode strikes the metal, a current flows causing the current control field to become energized which acts to raise the electrode thus establishing an arc. As the arc is lengthened, its voltage increases and the current decreases until a balance is established between the potential and current control fields. The third electrode is controlled in a similar manner.

As the charge melts and flows away, as the electrodes burn off, and as the scrap falls against the electrodes, the balance between the control fields is continually being disturbed. The Rototrol changes the generator voltage to position the electrodes to maintain the balance between arc voltage and current. This arrangement of the control field circuits corresponds to the accepted principle of regulation used successfully for many years in the balanced-beam type of regulator.

Provision is made for controlling the electrodes manually, either individually or in a group. This facilitates handling of the electrodes during charging, pouring, changing electrodes and so forth.

The wide range of current adjustment required is obtained by means of a rheostat shunting the current transformer. This method is used, since it maintains the same sensitivity for all current settings.

Figure 4 shows the arrangement of equipment in a single metal-enclosed unit. The front panel is arranged for setting into the wall of the furnace vault in the usual manner. Hinged side panels are used on the rear of the unit in order to provide maximum accessibility to the rotating equipment as well as to the panel mounted apparatus. A bolted rear plate is provided so that any one or all of the motor generator sets can be readily removed for inspection or maintenance. Enclosed machines with ball bearings are

A Method for Determining the Normal Modes of Foster's Reactance Networks

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Synopsis: A method is presented for finding the zeros of the impedance or admittance function of a Foster reactance network. More generally, the method is one of finding the zeros of a rational function having alternate zeros and poles, all of which are simple, if the poles are known; or of finding the poles if the zeros are known. The method consists of writing the function as a sum of partial fractions and replacing all but two of the fractions, depending on the zero to be found, by a constant and solving the resulting quadratic equation. In this way an upper and lower bound of a zero may be found, and, if need be, the method may be applied repeatedly to give a pair of bounds which will be as close to the required zero as necessary. The method has particular value if the rational function is the impedance or admittance function of one of Foster's reactance networks, since these functions are obtained directly from the networks as sums of partial fractions of the form $A_n/(B_n^2-\omega^2)$. Hence, the zeros may be obtained without reducing the function to the polynomial form. The new method may not always be shorter in application than other known methods, but its advantage lies in the facility with which an upper and lower bound of the required zero may be found and in the possibility of dealing with the partial-fraction form of the impedance or admittance function.

Statement of the Problem

THE two generalized networks of Figure 1 are Foster's canonic forms, which are of particular importance because either one is a possible representation of any reactance network.[1] They are forms in which the normal modes of oscillation are placed in evidence: for Figure 1a they coincide with the network meshes,[2] and for Figure 1b, which is the dual[3] of Figure 1a, the normal modes coincide with the network nodes. This means that if Figure 1a is excited by a voltage, or Figure 1b by a current, the response frequencies are merely those of the isolated sections. However, if the excitations of these networks are, respectively, current and voltage, they no longer have nodes or meshes which coincide with the normal modes. That is, the value of each natural frequency of oscillation depends on all the circuit constants of the network and cannot be determined from the properties of any one section. The problem to be treated is that of finding the normal modes for these circuits when exposed to the latter type of excitation. These solutions will also include the solutions for certain combinations of these networks, and also the dissipative case where all coils have the same R/L ratio and all capacitors have the same G/C ratio. The method will be illustrated in terms of the voltage excitation of Figure 1b, but with the application of the principles of duality[3] it applies also to the current excitation of Figure 1a. There are known methods for finding these normal modes, but under the special conditions of the circuits considered here the method to be introduced will provide good approximations with a minimum of effort.

Determination of the Normal Modes

On a mesh basis none of the possible meshes of Figure 1b satisfy the necessary conditions to make them coincide with the normal modes,[2] and if there are no such meshes the normal modes, regardless of the actual point of insertion of the disturbing voltage, are the values of p for which

$$Z(p)=0 \tag{1}$$

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used exclusively to eliminate periodic oiling, also to keep abrasive dust out of bearings. Individual motor generator sets having rugged construction and ample safety factors are employed. Should a failure of any unpredictable kind occur, the heat can be continued with two electrodes on automatic control and a spare set placed in service quickly.

The equipment is made so far as possible in one unit so that the various parts can be protected from dirt and physical damage. This also permits factory assembly and complete test, up to the operation of the electrode motors. A minimum of external connections and erection time is thus required.

Figure 5 is an oscillogram showing typical operation of the furnace during the melt-down period. This demonstrates the high speed of the electrodes. Unsustained sudden variations in current cause no extra action or undue wear on the equipment; but sustained changes cause immediate response. It will be noted that the electrode can be withdrawn at relatively high speed when a cave-in takes place. In case of abnormal unbalance between current and voltage in the arc, the equipment is able to perform its function with the minimum number of outages.

The start of the film shows a loss of voltage and current. The motor comes to rest in about one half of a second and remains at rest until the arc is re-established. In this case, the current was low and the Rototrol voltage immediately started rising in a direction to cause the electrode to be lowered. The motor started one seventh of a second after the change was initiated. This delay was due to the electrical and mechanical inertia of the system. The motor was accelerated rapidly to full speed, and, as the current approached the regulated value, the motor speed was diminished. At point 5-*A* there was a marked rise in current and the electrode motor was decelerated to zero speed and accelerated to one-third speed in the raise direction. It should be appreciated that during the melt-down period, electrical conditions in the furnace change rapidly, necessitating more or less continual electrode adjustment. This film covers a period of about ten seconds.

Figure 6 is an oscillogram showing the refining period. Here, relatively small changes in current take place, and consequently only small movement of electrodes is required. Because of the inertia of the electrical system, sudden unsustained variations do not cause movement of the electrodes.

Operating experience has shown that the Rototrol regulator combines the necessary degree of sensitivity, amplification, speeds of response, and stability required for arc-furnace operation. This regulator is particularly successful during melt-down periods when cave-ins frequently occur because the high speed withdrawal of the electrodes prevents breaker tripping because of sustained overload.

where p is the usual generalized angular velocity and $Z(p)$ is the impedance function of the network.[4]

For the network in question,

$$Z(p)=p\left(L_0+\sum_{n=1}^{N}\frac{A_n}{B_n^2+p^2}\right) \tag{2}$$

where

$$\left.\begin{aligned} A_n&=1/C_n && \text{(a)}\\ B_n&=1/\sqrt{L_nC_n} && \text{(b)}\end{aligned}\right\} \tag{3}$$

When there is a series capacitor, as shown in Figure 1b, it is a special case of the parallel combination for which the corresponding $B=0$, and is therefore included in the summation.

For the reactance network the values of p which satisfy equation 1 are pure imaginaries, and accordingly we are interested only in purely imaginary values of p Therefore, it is convenient to let

$$p=j\omega \tag{4}$$

and

$$Z(p)=Z(j\omega)=j\omega\left(L_0+\sum_{n=1}^{N}\frac{A_n}{B_n^2-\omega^2}\right) \tag{5}$$

It follows from the separation property of zeros and poles[1] of equation 5 that there will be a root of equation 1 between any two adjacent values of B_n. Let the values of B_n be arranged as an increasing sequence, with increasing subscripts, and choose the subscripts on the roots so that

$$B_1<\omega_1<B_2<\omega_2<\ldots<B_{k-1}<\omega_k<B_k<\ldots<\omega_N \tag{6}$$

All B's will be different because it is assumed that Figure 1b is reduced to its simplest form. The solution is obtained by solving

$$L_0+\phi(\omega)=0 \tag{7}$$

where

$$\phi(\omega)=\sum_{n=1}^{N}\frac{A_n}{B_n^2-\omega^2} \tag{8}$$

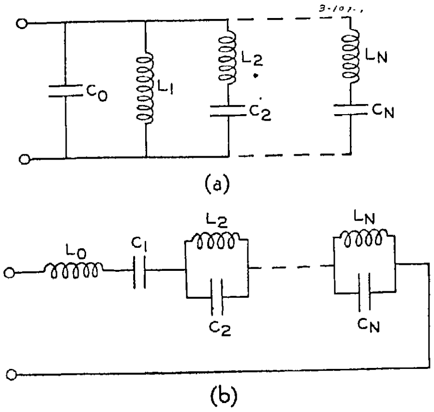

Figure 1. Foster's reactance networks

which contains all the roots except the trivial root $\omega=0$ which occurs if $B_1\neq 0$ and which has been factored out.

This is made clearer by the typical plot of $\phi(\omega)$, as shown in Figure 2. The function closely resembles a reactance function, since the latter is $\omega\phi(\omega)$. The intersections of these curves with the horizontal line $-L_0$ are the required roots. They are located symmetrically about the vertical axis, and therefore it is necessary to consider only the positive roots.

There are two procedures to be followed, depending on whether $1\leqq k\leqq N-1$ or $k=N$.

CASE 1. $1\leqq k\leqq N-1$

The root ω_k lies in the interval

$$B_k<\omega_k<B_{k+1} \tag{9}$$

and with this knowledge we can separate out the two terms of $\phi(\omega)$ which have poles nearest to ω_k and approximate the remaining terms by a term which will be a constant. Equation 7 becomes

$$\frac{A_k}{B_k^2-\omega^2}+\frac{A_{k+1}}{B_{k+1}^2-\omega^2}\equiv\psi(k,\omega)=W(k) \tag{10}$$

which serves to define $\psi(k, \omega)$, and where W is a constant. The solution of equation 10 is

$$\left.\begin{aligned} \omega^2&=\frac{1}{2W}\,[F(k)+G(k)], \quad W\neq 0 && \text{(a)}\\ &=\frac{B_{k+1}{}^2A_k+B_k{}^2A_{k+1}}{A_{k+1}+A_k}, \quad W=0 && \text{(b)}\\ &\qquad 1\leqq k\leqq N-1\\ &\text{where}\\ F(k)&=W(B_{k+1}{}^2+B_k{}^2)-(A_{k+1}+A_k) && \text{(c)}\\ &\text{and}\\ G(k)&=\sqrt{(A_{k+1}+A_k)^2-2W(B_{k+1}{}^2-B_k{}^2)^2(A_{k+1}-A_k)+W^2(B_{k+1}{}^2-B_k{}^2)^2} && \text{(d)}\end{aligned}\right\} \tag{11}$$

The form of $\psi(k, \omega)$ is shown in Figure 3, and it is seen from this that there may be an extraneous root outside of the interval B_k to B_{k+1}. However, equation 11 is written in such a form as to always give a root within the required interval. In speaking of a root of equation 10 it will always be understood that it is the root given by equation 11. It is evident from the form of $\psi(k, \omega)$ that there will always be a root, regardless of the value of W. The problem is now one of finding suitable values of W. To do this we write equation 7 as

$$\left.\begin{aligned} \psi(k,\omega)&=S(k,\omega) && \text{(a)}\\ &\text{where}\\ S(k,\omega)&=-L_0-\sum_{\substack{n=1\\ n\neq k,\,k+1}}^{N}\frac{A_n}{B_n^2-\omega^2} && \text{(b)}\end{aligned}\right\} \tag{12}$$

$S(k, \omega)$ is shown in Figure 3, and it is apparent that in the interval between B_k and B_{k+1}, $\psi(k, \omega)$ is continuous and always increasing, and that $S(k, \omega)$ is continuous, always decreasing, and is bounded. Thus, if ω_p is any value, $B_k\leqq\omega_p\leqq B_{k+1}$, and we define

$$W=S(k,\omega_p) \tag{13}$$

there will be a solution of equation 10 which we will call ω_q and which will have the following properties:

$$\text{if }\begin{Bmatrix}\omega_p>\omega_k\\ \omega_p=\omega_k\\ \omega_p<\omega_k\end{Bmatrix}\text{ then }\begin{Bmatrix}\omega_q<\omega_k\\ \omega_q=\omega_k\\ \omega_q>\omega_k\end{Bmatrix} \tag{14}$$

The properties of $\psi(k, \omega)$ and $S(k, \omega)$ also show that if $B_k\leqq\omega_p'<\omega_p\leqq B_{k+1}$, and ω_q' is the root corresponding to ω_p', as given by equations 11 and 13, then

$$B_k<\omega_q<\omega_q'<B_{k+1} \tag{15}$$

In view of these relations we will find it expedient to define

$$\left.\begin{aligned} W_1(k)&=S(k,B_k) && \text{(a)}\\ W_2(k)&=S(k,B_{k+1}) && \text{(b)}\end{aligned}\right\} \tag{16}$$

and let the corresponding roots of equation 10 be $\bar{\omega}_k$ and $\underline{\omega}_k$, respectively. From relations 9, 14, and 15 it follows that

$$B_k<\underline{\omega}_k<\omega_k<\tilde{\omega}_k<B_{k+1} \tag{17}$$

and we have determined upper and lower bounds of ω_k which are closer than B_k and B_{k+1}. If this approximation is not sufficient, one may use $\underline{\omega}_k$ and $\bar{\omega}_k$ in place of B_k and B_{k+1}, respectively, and determine two new roots, $\bar{\omega}_k'$ and $\underline{\omega}_k'$.

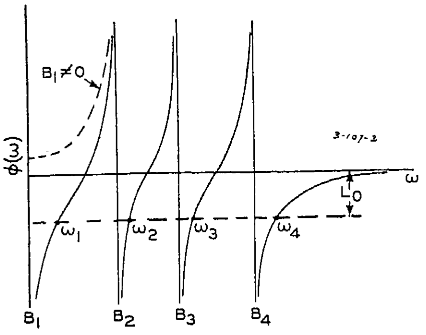

Figure 2. Plot of $\phi(\omega)$ versus ω to show the locations of the roots. Shown for N=4

This process may be repeated as many times as required. To be more specific, define

$$\left.\begin{array}{lll} W_1^{(s)}(k)=S[k,\underline{\omega}_k^{(s-1)}] & \multirow{2}{*}{$s\geqq 1$} & (a) \\ W_2^{(s)}(k)=S[k,\bar{\omega}_k^{(s-1)}] & & (b) \end{array}\right\} \quad (18)$$

and let $\bar{\omega}_k^{(s)}$ and $\underline{\omega}_k^{(s)}$ be the roots of equation 10 using equations 18*a* and *b*, respectively, for W. Consider the case $s=1$. From relations 15 and 17 it follows that $\bar{\omega}_k'<\bar{\omega}_k$ and $\underline{\omega}_k'>\underline{\omega}_k$, and from relations 14 and 17 we obtain the relations $\bar{\omega}_k'>\omega_k$ and $\underline{\omega}_k'<\omega_k$. Hence,

$$B_k<\underline{\omega}_k<\underline{\omega}_k'<\omega_k<\bar{\omega}_k'<\bar{\omega}_k<B_{k+1} \quad (19)$$

It is obvious that this reasoning may be continued, leading to the pair of sequences

$$\left.\begin{array}{l} \bar{\omega}_k>\bar{\omega}_k'>\bar{\omega}_k''>\ldots>\bar{\omega}_k^{(s-1)}>\bar{\omega}_k^{(s)}>\ldots>\omega_k \\ \underline{\omega}_k<\underline{\omega}_k'<\underline{\omega}_k''<\ldots<\underline{\omega}_k^{(s-1)}<\underline{\omega}_k^{(s)}<\ldots<\omega_k \end{array}\right\} \quad (20)$$

Observe that relation 20 contains twice as many values as required to obtain a given approximation. For example, if $\bar{\omega}_k$ is found one can immediately find $\underline{\omega}_k'$, and then $\bar{\omega}_k''$, and so forth. Similarly, $\underline{\omega}_k$ could be used as the starting point, and thus, either of the following pairs of sequences may be used:

$$\left.\begin{array}{l} \left.\begin{array}{l} \bar{\omega}_k>\bar{\omega}_k''>\ldots>\bar{\omega}_k^{(s-2)}>\bar{\omega}_k^{(s)}> \\ \qquad\qquad\ldots>\omega_k \\ \underline{\omega}_k'<\underline{\omega}_k'''<\ldots<\underline{\omega}_k^{(s-1)}<\underline{\omega}_k^{(s+1)}< \\ \qquad\qquad\ldots<\omega_k \end{array}\right\} (a) \\ \text{or} \\ \left.\begin{array}{l} \bar{\omega}_k'>\bar{\omega}_k'''>\ldots>\bar{\omega}_k^{(s-1)}>\bar{\omega}_k^{(s+1)}> \\ \qquad\qquad\ldots>\omega_k \\ \underline{\omega}_k<\underline{\omega}_k''<\ldots<\underline{\omega}_k^{(s-2)}<\underline{\omega}_k^{(s)}< \\ \qquad\qquad\ldots<\omega_k \end{array}\right\} (b) \end{array}\right\} \quad (21)$$

CASE 2. $k=N$

This case requires special treatment because there is no pole of $\phi(\omega)$ beyond B_n, and ω_N is in the range

$$B_N<\omega_N<\infty \quad (22)$$

In contrast to the previous practice, it is not possible to keep two terms, having poles on opposite sides of ω_N, in their exact form and replace the remainder of $\phi(\omega)$ by an approximation. One could, therefore, keep only one term, $A_N\div(B_N^2-\omega^2)$, as the exact term. However, since the solution can be given explicitly if two exact terms are used, a more accurate approximation will be obtained by also using the term $A_{N-1}/(B_{N-1}^2-\omega^2)$.

One is lead to attempt to approximate the other terms by a constant, as was done in case 1. That is, we will seek a suitable value of W to make the solution of

$$\psi(N-1,\omega)=W(N) \quad (23)$$

approximate ω_N. $\psi(N-1,\omega)$ is shown in Figure 4 from which it is seen that for equation 23 to have a solution it is necessary that $W<0$. Assuming this to be true the solution is

$$\left.\begin{array}{ll} \omega^2=\dfrac{1}{2W}[F(N)-G(N)], \quad W<0 & (a) \\ \text{where} & \\ F(N)=W(B_N^2+B_{N-1}^2)- & \\ \qquad\qquad (A_N+A_{N-1}) & (b) \\ \text{and} & \\ G(N)=\sqrt{\begin{array}{l}(A_N+A_{N-1})^2-2W(B_N^2- \\ B_{N-1}^2)^2(A_N-A_{N-1})+ \\ \quad W^2(B_N^2-B_{N-1}^2)^2\end{array}} & (c) \end{array}\right\} \quad (24)$$

This is written in such a way as to give the solution which is greater than B_N. The root given by equation 24 will henceforth be spoken of as the root of equation 23. Continuing, as for case 1, we write equation 7 as

$$\psi(N-1,\omega)=S(N-1,\omega) \quad (25)$$

and examine $S(N-1,\omega)$ in the interval of ω for which it may serve as a value of W. For $\omega>B_N$, $S(N-1,\omega)$ is continuous and always decreasing, with increasing ω, and becomes less than zero at some point, ω_r, as indicated in Figure 4. This point may lie on either side of B_N. If ω_s is the larger of ω_r and B_N, and if W is defined as

$$W=S(N-1,\omega_t) \quad (26)$$

where $\omega_s<\omega_t<\infty$, then there will be a solution of equation 23 for this value of W. Let this solution be ω_u. From the properties of $\psi(N-1,\omega)$ and $S(N-1,\omega)$ one may conclude that

$$\text{if}\ \left\{\begin{array}{l}\omega_t>\omega_N \\ \omega_t=\omega_N \\ \omega_t<\omega_N\end{array}\right\}\ \text{then}\ \left\{\begin{array}{l}\omega_u<\omega_N \\ \omega_u=\omega_N \\ \omega_u>\omega_N\end{array}\right\} \quad (27)$$

and if $\omega_s\leqq\omega_t'<\omega_t\leqq\infty$ and ω_u' is the root corresponding to ω_t', as given by equations 26 and 24, then

$$\omega_s<\omega_u<\omega_u'<\infty \quad (28)$$

The procedure to be used in obtaining a solution depends on the position of ω_r relative to B_N. For the first case, assume $S(N-1,B_N)<0$, which is equivalent to the condition $\omega_r<B_N$. This is similar to case 1 and accordingly we define

$$\left.\begin{array}{ll} W_1(N)=S(N-1,B_N) & (a) \\ W_2(N)=S(N-1,\infty)=-L_0 & (b) \end{array}\right\} \quad (29)$$

The corresponding roots obtained by using equations 29*a* and *b*, respectively, will be $\bar{\omega}_N$ and $\underline{\omega}_N$, and in view of relations 22, 27, and 28 they will satisfy the inequality

$$B_N<\underline{\omega}_N<\omega_N<\bar{\omega}_N \quad (30)$$

Closer approximations may be obtained, exactly as in case 1, by defining

$$\left.\begin{array}{lll} W_1^{(s)}(N)=S[N-1,\underline{\omega}_N^{(s-1)}] & \multirow{2}{*}{$s\geqq 1$} & (a) \\ W_2^{(s)}(N)=S[N-1,\bar{\omega}_N^{(s-1)}] & & (b) \end{array}\right\} \quad (31)$$

and $\bar{\omega}_N^{(s)}$ and $\underline{\omega}_N^{(s)}$ as the roots of equation 23 using, respectively, equations 31*a* and *b* for W. For $s=1$, in consequence of relations 28 and 30, we have $\bar{\omega}_N'<\bar{\omega}_N$ and $\underline{\omega}_N'>\underline{\omega}_N$ and from relations 27 and 30, $\bar{\omega}_N'>\omega_N$ and $\underline{\omega}_N'<\omega_N$. Therefore we may write the inequality

$$B_N<\underline{\omega}_N<\underline{\omega}_N'<\omega_N<\bar{\omega}_N'<\bar{\omega}_N \quad (32)$$

and by extending this repeatedly there results the pair of sequences,

$$\left.\begin{array}{l} \bar{\omega}_N>\bar{\omega}_N'>\bar{\omega}_N''>\ldots>\bar{\omega}_N^{(s-1)}>\bar{\omega}_N^{(s)}> \\ \qquad\qquad\ldots>\omega_N \\ \underline{\omega}_N<\underline{\omega}_N'<\underline{\omega}_N''<\ldots<\underline{\omega}_N^{(s-1)}<\underline{\omega}_N^{(s)}< \\ \qquad\qquad\ldots<\omega_N \end{array}\right\} \quad (33)$$

from which either of the two following pairs may be selected for computation:

$$\left.\begin{array}{l} \left.\begin{array}{l} \bar{\omega}_N>\bar{\omega}_N''> \\ \ldots>\bar{\omega}_N^{(s-2)}>\bar{\omega}_N^{(s)}>\ldots>\omega_N \\ \underline{\omega}_N'<\underline{\omega}_N'''< \\ \quad\ldots<\underline{\omega}_N^{(s-1)}<\underline{\omega}_N^{(s+1)}< \\ \qquad\qquad\ldots<\omega_N \end{array}\right\} (a) \\ \left.\begin{array}{l} \bar{\omega}_N'>\bar{\omega}_N'''> \\ \quad\ldots>\bar{\omega}_N^{(s-1)}>\bar{\omega}_N^{(s+1)}> \\ \qquad\qquad\ldots>\omega_N \\ \underline{\omega}_N<\underline{\omega}_N''< \\ \ldots<\underline{\omega}_N^{(s-2)}<\underline{\omega}_N^{(s)}<\ldots<\omega_N \end{array}\right\} (b) \end{array}\right\} \quad (34)$$

With the introduction of the sequences 34*b* there is some relaxing of the original condition, that $W_1(N)<0$. To use these sequences it is necessary only to have $W_1'(N)<0$, which is equivalent to $\underline{\omega}_N>\omega_r$.

If $W_1'(N)\geqq 0$, an upper bound of ω_N may be found by another method which consists of obtaining a root of

$$\psi(N-1,\ \omega)=-L_0-\sum_{n=1}^{N-2}\frac{A_n}{B_{N-1}^2-\omega^2} \quad (35)$$

It is easy to see that equation 35 has a root, by writing it as

$$\left.\begin{array}{ll} \dfrac{A_{N-1}'}{B_{N-1}^2-\omega^2}+\dfrac{A_N}{B_N^2-\omega^2}=-L_0 & (a) \\ \text{where} & \\ A_{N-1}'=\displaystyle\sum_{n=1}^{N-1}A_n & (b) \end{array}\right\} \quad (36)$$

Let this root be designated by $\bar{\omega}_N^*$, and observe that, if $\omega>B_N$, then

$$-\sum_{n=1}^{N-2}\frac{A_n}{B_{N-1}^2-\omega^2}>-\sum_{n=1}^{N-2}\frac{A_n}{B_n^2-\omega^2} \quad (37)$$

From this, and the fact that $\psi(N-1,\omega)$ is increasing with increasing ω, it may be concluded that

$$\bar{\omega}_N^*>\omega_N \quad (38)$$

Thus $\bar{\omega}_N^*$, an upper bound of ω_N, has been found by a method which is applicable in all cases.

It is of interest to note, parenthetically, that if $W_1'(N) < 0$ then $\bar{\omega}_N^*$ and $\bar{\omega}_N'$ can both be found, and if there is much difference between them it may be worth the extra computation required to know which is the closer to ω_N and therefore which one may be most profitably used as the basis for future computations of the sequences 34. It is possible to know which is the better approximation by computing $\bar{\omega}_N^*$ and then either also computing ω_N' and comparing directly with $\bar{\omega}_N^*$, or comparing $S(N-1, \underline{\omega}_N)$ with

$$-L_0 - \sum_{n=1}^{N-2} A_n/(B_{N-1}^2 - \bar{\omega}_N^{*2})$$

If the former is larger, then $\bar{\omega}_N' > \bar{\omega}_N^*$, and vice versa.

To return to the case where $W_1'(N) \geqq 0$, $\bar{\omega}_N^*$ having been found, it may be used to define

$$W_2^*(N) = S(N-1, \bar{\omega}_N^*) \qquad (39)$$

If the root of equation 23, when $W_2^*(N)$ is used, is $\underline{\omega}_N^*$, it follows from relations 27 and 38 that $\underline{\omega}_N^* < \omega_N$. Also, $S(N-1, \bar{\omega}_N^*) > S(N-1, \infty)$ and therefore, from the increasing property of $\psi(N-1, \omega)$,

$$\underline{\omega}_N < \underline{\omega}_N^* < \omega_N \qquad (40)$$

Although $\underline{\omega}_N < \omega_r$ it is possible that $\underline{\omega}_N^* > \omega_r$, in which event $\underline{\omega}_N^*$ is a possible starting point for the sequences 34, as a replacement for either B_N or $\underline{\omega}_N$. Whether this condition is fulfilled may be checked by observing if $S(N-1, \omega_N^*)$ is less than zero, this being the necessary and sufficient condition to make $\underline{\omega}_N^* > \omega_r$.

If this attempt at finding an improved upper bound, by first seeking a lower bound which is greater than ω_r, is not successful, the following procedure may be used. Arbitrarily choose a value, ω_N^*, such that

$$\left.\begin{array}{ll} \underline{\omega}_N^* < \omega_N^* < \bar{\omega}_N^* & \text{(a)} \\ \text{and} & \\ S(N-1, \omega_N^*) < 0 & \text{(b)} \end{array}\right\} \quad (41)$$

If ω_N^* is less than ω_N, it may be used as B_N or $\underline{\omega}_N$ in forming the sequences 34. To find whether this is true use $S(N-1, \omega_N^*)$ as W in equation 23 and call the resulting root $\omega_N^{*\prime}$. From relation 27, if

$$\left.\begin{array}{ll} \omega_N^{*\prime} > \omega_N^* & \text{(a)} \\ \text{then} & \\ \omega_N^* < \omega_N & \text{(b)} \end{array}\right\} \quad (42)$$

Also, from relation 27, if

$$\left.\begin{array}{ll} \omega_N^{*\prime} < \omega_N^* & \text{(a)} \\ \text{then} & \\ \omega_N^{*\prime} < \omega_N & \text{(b)} \\ \text{and if} & \\ S(N-1, \omega_N^{*\prime}) < 0 & \text{(c)} \end{array}\right\} \quad (43)$$

$\omega_N^{*\prime}$ may be used as B_N or ω_N in relation 34. Thus, there are two possibilities: if relation 42*a* is true the search for a lower bound is completed, and the same conclusion follows if relation 43*a* and *c* are both true. However, if relation 43*a* is true but 43*c* is not true then another, smaller, value will have to be assumed for ω_N^*.

Application to Other Circuits

In Figure 5 two circuits are shown, to which this method of determining the normal modes is applicable. Consider Figure 5b, with a current excitation, a voltage excitation being no different than the previous problem and of no further interest. The normal modes will be the values of p which make

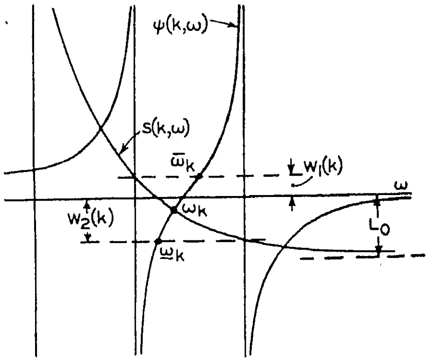

Figure 3. Properties of the functions involved in finding all the roots except the largest

$$Y(p) = 0 \qquad (44)$$

From the form of the circuit it is apparent that

$$Y(p) = \frac{Z_A(p) + Z_B(p)}{Z_A(p) Z_B(p)} \qquad (45)$$

and hence, if p_k is a root of

$$Z_A(p) + Z_B(p) = 0 \qquad (46)$$

it is also a root of equation 44 unless $Z_A(p_k) = Z_B(p_k) = 0$. The roots of equation 46 can be found by the method which has been presented. Likewise, for Figure 5a the normal modes are the roots of

$$Y_A(p) + Y_B(p) = 0 \qquad (47)$$

and, as has been indicated, the formal solution of equation 47 is the same as the solution of equation 46.

All of these networks can be treated equally well if all coils have the same R/L ratio and all capacitors the same G/C ratio. The general case of this is treated by Guillemin[5] who shows that if

$$\alpha = R/2L \quad \text{and} \quad \beta = G/2C \qquad (48)$$

then, for roots of $Z(p) = 0$, we obtain

$$\left.\begin{array}{ll} p_k = -(\alpha + \beta) \pm j\omega_k & \text{(a)} \\ \text{where} & \\ \omega_k = \sqrt{g_k^2 - (\alpha - \beta)^2} & \text{(b)} \end{array}\right\} \quad (49)$$

and where g_k is the value that p_k would have if there were no dissipation.

Application to Other Rational Functions

An impedance or admittance function of a reactance network is a special rational function which has everywhere a positive slope and, consequently, alternating poles and zeros all of which are simple. The poles come in pairs located symmetrically about the point $\omega = 0$, and the residue at one of the poles is the negative of the residue at its mate. This makes it possible, for such a function, to combine the terms representing the two poles of a pair into one term and only treat the function for $\omega > 0$. The fact that the zeros come in pairs, located symmetrically about the point $\omega = 0$, is indicated by equations 10 and 24 because there the zeros are given in terms of ω^2. In the general case of a rational function having alternate poles and zeros, all of which are simple, the same procedure may be used to find upper and lower bounds of the zeros, but without the possibility of combining terms. If the poles and residues are known the approximate zero will be a solution of an equation of the form

$$\frac{A_k}{B_k - \omega} + \frac{A_{k+1}}{B_{k+1} - \omega} = W \qquad (50)$$

where W is defined in similarity with equations 12 and 13 but where the summation must be over all the poles except B_k and B_{k+1}. Equation 50 is a quadratic in ω, and its solution is given by equations 11, but with ω^2 replaced by ω. The treatment for the greatest and smallest zeros will be similar to that given under case 2, with the aforementioned modifications.

It should be pointed out that it is necessary to know only the locations of the poles since the residues may be found by explicit methods.[6] Also, if the locations of the zeros of a function are known, the poles may be found by applying the method to the reciprocal of the function.

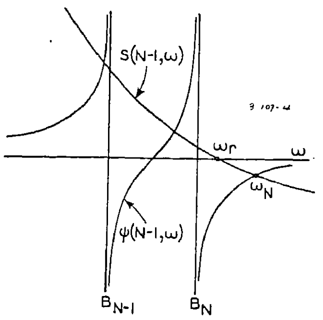

Figure 4. Properties of the functions involved in finding the largest root

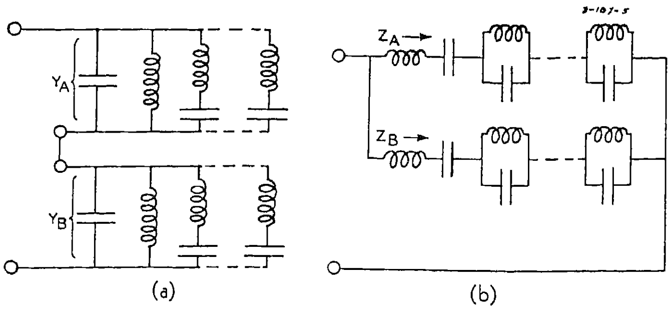

Figure 5. Combinations of the reactance networks to which the method is applicable

Therefore, this method may be used to find the zeros or poles, provided that the poles or zeros, respectively, are known.

Conclusions

In finding the zeros of a rational function by the method presented, one finds a sequence of approximate values, each approximate value being obtained from the previous one. The work involved in finding an approximate value is about the same as in substituting a trial value in the original function, because the evaluation of W requires the substitution of the previous approximate value in all but two of the partial fraction terms which constitute the function. Therefore, probably the greatest advantage of the new method is in the possibility of obtaining, with only two sets of computations, a pair of reasonably close approximate values between which the exact zero is known to be located. In many cases the sequences will converge so rapidly that only two terms need be computed, even for good accuracy. If it is necessary to compute more terms of the sequences to improve the accuracy, this method may have less advantage over the trial method, especially if the initial trials should be close to the zeros to be found. However, the new method does not depend on initial chance assumptions, and it is probable that it will at least yield a solution as quickly, on the average, as the trial method, and perhaps much more quickly. There may be some doubt in the reader's mind as to whether these comments apply to the extreme zeros because of the possible lengthened procedure in this case, which may even require a trial substitution. However, the result of the substitution of this trial value is used directly to find a closer approximation, and also, since a lower and upper bound may always be found by explicit means, the range of choice of the trial value is restricted to the interval between these bounds. This possible increased difficulty in finding these zeros is not without its counterpart in the trial method, because the range of choice of trial values extends to infinity, and hence the probability of a good choice is more remote than for the other more restricted zeros.

Of course, the trial method is not the only alternative for finding the zeros of a rational function. They may be obtained by converting the function to the polynomial form and using a method which is applicable to a polynomial, such as Horner's or Graffe's method, or a method suggested by Higgins.[7] However, if the rational function is the impedance or admittance function for the circuits considered here, it will automatically come in the partial-fraction form, and, if there is a large number of terms in the partial-fraction expression, there may be considerable work required to reduce it to the polynomial form. In this case the new method may be preferable, depending on how many of the total number of roots are to be found. This is because the reduction to the polynomial form need be carried out only once, and the work required to find N zeros is therefore less than N times the work required to find the first zero; whereas in the partial-fraction method the work required to find each zero is approximately the same. Hence, the smaller the number of zeros to be found the greater will be the advantage of the partial-fraction method.

References

1. A Reactance Theorem, **R. M. Foster.** *Bell System Technical Journal*, 3, 1924, pages 259-67.

2. Making Normal Co-ordinates Coincide With the Meshes of an Electrical Network, **E. A. Guillemin.** *Proceedings* of the Institute of Radio Engineers, 12, 1927, page 935.

3. Electric Circuits (book), Massachusetts Institute of Technology staff. John Wiley and Sons, Inc., New York, N. Y. Pages 245-6.

4. Operational Circuit Analysis (book), **Vannevar Bush.** John Wiley and Sons, Inc., New York, N. Y. Page 45.

5. Communication Networks (book), **E. A. Guillemin.** John Wiley and Sons, Inc., New York, N. Y. Volume II, page 245.

6. Reference 4, page 89.

7. Note on Whittaker's Method for the Roots of a Power Series, **J. T. Higgins.** *American Mathematical Monthly*, volume 49, 1942, pages 462-5.

Load Pickup by a Group of Ignitron Rectifiers

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Synopsis: One of the advantages of the application of mercury-arc rectifiers to large electrochemical loads, such as aluminum cell lines, is that, if all the rectifiers are excited simultaneously, they will pick up and share the load together. This eliminates need for large d-c cell-line circuit breakers.

The conventional method of starting simultaneously several ignitron mercury-arc rectifiers, widely used in large electrochemical plants, has been to energize the individual ignitor firing circuits from a master starting switch. Differences in closing time of the control contactors were reflected in proportional irregularity of load pickup between the ignitron rectifiers.

Recent tests, described in this paper, show that the grids usually employed in the ignitrons for anode shielding can be used as an alternative method to energize cell lines. If all the grids are biased negatively, they will prevent current flowing from the rectifiers while the firing circuits are all energized and the switches closed in the power circuits. Then, by operating one switch or a plurality of accurately timed switches, all grids can be made positive at the same instant with the result that all the rectifiers pick up load at the same instant and share it equally.

MANY of the phenomena of the behavior of large rectifier installations, during the instant of applying or dropping electrochemical loads, have been little understood or investigated by engineers. Certain field tests were made in large aluminum reduction plants, the results of which add to the engineering knowledge relating to this subject. Technical advance in the ever-expanding electrochemical field, even though it involves simply a better understanding of the operation of the electrical apparatus in the plant, is important to the engineer. An analysis of the problem and a report of the field tests are contained in this paper.

Among the big five electrochemicals, aluminum, magnesium, chlorine, copper, and zinc, some of the pioneering work and operating experience in connection with aluminum plants stands out because of the tremendous increase in tonnage of aluminum which industry has demanded, particularly for the airplane program. The electrical apparatus required in the reduction of alumina to pure aluminum in the electrolytic cell lines is, however, typical in a great many respects of what is required for the other electrochemicals.

Arrangement of Ignitron Rectifiers

For purposes of this paper, it is necessary to review briefly the heavy electrical equipment involved in a typical large rectifier installation, such as is used for the reduction of aluminum.[1–4] Figure 1 shows a typical one-line power diagram for an aluminum installation. Conversion apparatus for one cell line, for instance, is rated 38,700 kw, 645 volts, 60,000 amperes direct current. The individual electrolytic cells are all connected in series, and each carries the full cell-line current, which under normal conditions may range as high as 50,000 to 55,000 amperes. Since it is impractical to build a power rectifier in a single unit of such large capacity, the conversion apparatus must be broken up into several units. So far, in the United States it has been found impractical to build rectifier transformers in the 600-volt d-c output level larger than 10,000 amperes direct current. Likewise, the largest single rectifier assembly in the 600-volt d-c level is approximately 5,000 to 6,000 amperes direct current. For these reasons, the typical 60,000-ampere rectifier installation is, therefore, arranged as shown in Figure 1 with six separate transformers, each feeding two ignitron-rectifier assemblies.

The fact that single large rectifier installations of this sort must be broken up into several units, however, has its advantages. Since there usually is provided a small amount of extra rectifier and transformer capacity, the scheme is flexible from the maintenance standpoint. For instance, it is usually possible to maintain full current input and, consequently, full production when one unit consisting of a transformer and two rectifier assemblies are out of service for maintenance. For the latter reason, and in order to provide protection from faults and arc-back, power circuit breakers and isolating disconnect switches always are provided.

Earlier large electrochemical cell lines, which were placed in operation before the successful development of mercury-arc rectifiers, were fed from several motor-generators or synchronous converters.[5–7] Since the largest ratings in rotating conversion apparatus compare with the present maximum ratings of single assemblies of power-rectifier equipment, earlier arrangements for connecting the several units to the main d-c bus were very similar to current practice.

Problem of Energizing Cell Line

The method used for energizing large electrolytic cell lines from a battery of several generators or rotary converters has a bearing on the subject of this paper. It was first necessary to bring the generators or converters up to speed. Next, their d-c output voltages were equalized. Since it always has been impossible to build high accuracy in the closing time of d-c air circuit breakers, it was necessary to connect all the separate converting units to the main d-c substation bus before the cell-line load was connected to the bus. This requirement existed because the first machine, whose d-c breaker closed, tended to pick up the complete cell-line load. Since the cell-line load is many times the individual rating of the conversion units, its d-c air circuit breaker would immediately trip on overload. The d-c circuit breakers on the rest of the machines also would clear instantly as they

Table I

Location	Test Numbers	Time After Master Starting Switch Was Closed Before the First Rectifier Started	Maximum Difference in Pickup Time Between First and Last Rectifier Starting
Plant *A*	1	4 cycles	$1^3/_4$ cycles
Plant *A*	2	4 cycles	$2^1/_8$ cycles
Plant *A*	3	$3^1/_8$ cycles	$1^3/_8$ cycles
Plant *A*	4	$3^1/_2$ cycles	$^3/_4$ cycle
Plant *A*	5	$4^1/_8$ cycles	$1^1/_4$ cycles
Plant *B*	1	$4^1/_2$ cycles	$1^1/_2$ cycles
Plant *C*	1	6 cycles	$1^3/_4$ cycles

In the interval between making plant *A* test 2 and plant *A* test 3, the contactor gap spacings on all 12 ignitor-firing-circuit contactors were readjusted to determine the effects of different contactor closing time on the performance of the various rectifiers. This resulted in changing three factors. First, the time was shortened for picking up the first rectifier. Second, the maximum difference in pickup time between first and last rectifier to start was decreased. Finally, the identity of the first rectifier to pick up load was changed.

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closed in succession. For these reasons the method has never been employed.

As an alternative method, a main d-c circuit breaker, capable of closing in on and carrying the full-load line current, was always employed with rotating converting equipment. This latter breaker was closed only after all the individual units had been connected to the station bus and the full capacity required to carry the load continuously was available.

Obviously, the elimination of this expensive circuit breaker designed to carry and rupture these large and highly inductive loads is desirable.

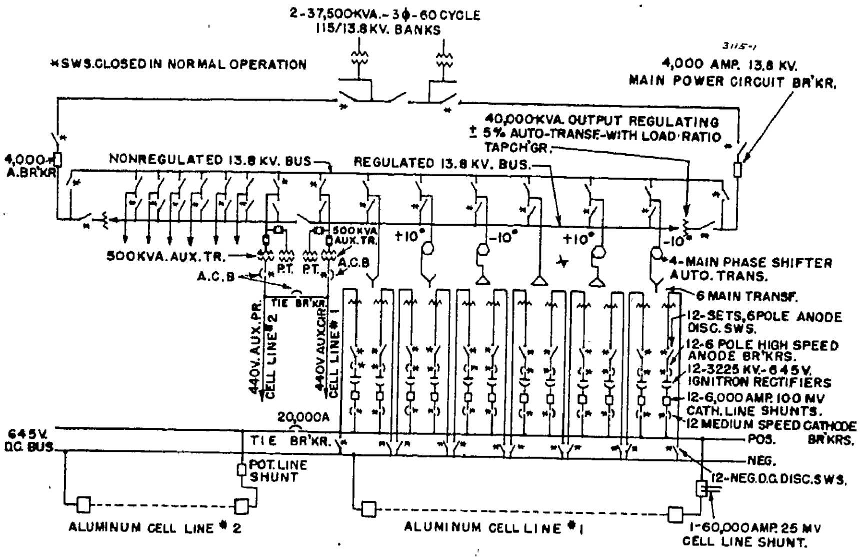

2-37,500KVA.-3ϕ-60 CYCLE
115/13.8KV. BANKS
*SWS.CLOSED IN NORMAL OPERATION
3115-1
4,000 AMP. 13.8 KV.
MAIN POWER CIRCUIT BR'KR.
40,000KVA. OUTPUT REGULATING
± 5% AUTO-TRANSF.-WITH LOAD RATIO
TAPCH'GR.
4,000 A.BR'KR
NONREGULATED 13.8 KV. BUS
REGULATED 13.8 KV. BUS.
500KVA AUX.TR.
+10°
-10°
+10°
-10°
500KVA. AUX. TR.
P.T.
P.T.
A.C.B
A.C.B
4-MAIN PHASE SHIFTER AUTO. TRANS.
6 MAIN TRANSF.
TIE BR'KR.
440V. AUX. PR. CELL LINE #2
440V AUX.CIR. CELL LINE #1
12-SETS, 6POLE ANODE DISC. SWS.
12-6 POLE HIGH SPEED ANODE BR'KRS.
12-3225 KV.-645V. IGNITRON RECTIFIERS
12-6,000 AMP. 100 MV CATH. LINE SHUNTS.
12 MEDIUM SPEED CATHODE BR'KRS.
20,000A
645V. D.C. BUS
TIE BR'KR.
POS.
NEG.
POT. LINE SHUNT
12-NEG. D.C. DISC. SWS.
ALUMINUM CELL LINE # 2
ALUMINUM CELL LINE #1
1-60,000 AMP. 25 MV CELL LINE SHUNT.

Figure 1. Typical one-line power diagram for cell lines, Aluminum Company of America

One of the advantages of mercury-arc rectifiers over rotating equipment is their ability to pick up electronically such large loads after all the power switching has been completed. There are two general ways of accomplishing this:

1. By controlling the arc in the rectifier with grids.
2. By controlling it with ignitors.

Load Application by Ignitor Control

A widely used method of energizing large cell-line loads supplied by ignitron rectifiers has been to close the a-c and d-c power circuit breakers before exciting the ignitors in the cathode mercury. This means that no current flows from the rectifier, even though all the power circuits are closed, because the ignitors have not caused cathode spots. Magnetic-impulse firing circuits are used widely in such electrochemical service.[8,9] Most magnetic-impulse firing circuits commercially available start generating impulse voltages almost instantly after the contactors in their supply lines are closed and suitable a-c voltage is applied. Advantage has been taken of this performance of the ignitor firing circuits for starting all the rectifiers in a substation at once in order to pick up the large cell-line current.

Where as many as 12 ignitron rectifiers feed a common d-c bus, a master a-c excitation circuit has been used. This has consisted of a 12-pole relay or the equivalent. Each of the 12 poles is connected to the closing coil of a three-pole contactor in the a-c supply circuit to the firing equipment of each of the rectifiers. A single-pole master control switch located in the coil circuit of the 12-pole relays can, therefore, be closed to pick up all of the 12 separate a-c contactors in the supply circuits to the firing equipments.

The procedure, therefore, in picking up ignitron-fed cell-line loads after outages has been rather simple. First, the master excitation switch is opened, which releases all the a-c contactors in the individual firing-circuit supplies. Under this condition, none of the ignitors in any of the rectifiers can create cathode spots to cause current to flow. Next, all of the a-c and d-c power circuit breakers and switches are closed. So far no current flows. The final step then is to close the master a-c excitation circuit energizing all of the separate ignitor firing circuits.

Limitations of This Widely Used Starting Method

The master a-c excitation method of picking up large electrochemical loads fed from ignitron rectifiers has been used for years. As operating experience was gained, it appeared, however, that there were certain limitations. Outages have occurred occasionally because of loss of main a-c supply voltage. Sometimes, when the cell lines were re-energized, one or more of the cathode breakers would trip out from overcurrent. There appeared to be two possible causes for this action:

1. Standard commercial a-c contactors (three-pole) were used in the supply circuits to the individual ignitor firing equipments. While the pickup time of these contactors for most applications is satisfactorily uniform, it was realized that some disparity must exist between the various contactors in closing action. Since cell lines have inductance, it had been anticipated by the application engineers first dealing with the problem that the rate of rise of the cell-line current would be low enough to permit such inequalities in contactor closing time as might appear. Whether this assumption was correct now seemed open to some doubt until tests could be made to substantiate it under field conditions.

2. Resistance characteristics of ignitor firing points are different when the ignitors and liquid mercury in the cathode pools are cold from what they are during operation when the ignitors and mercury are hot. It appeared possible that when the ignitors were cold, such as is the case after a prolonged shutdown, there might be some delay in creating cathode spots in the ignitron rectifiers after the ignitor firing circuits were energized, until the ignitors warmed up and their resistance characteristics improved.

Grid Starting Method

A careful study of the performance of the rectifiers and ignitor firing circuits, was, of course, indicated under the circumstances. It was obvious that satisfactory data could be obtained only under actual field conditions. In anticipation that one or the other, or both, of the foregoing limitations would prove true, an alternative method for starting large ignitron installations feeding electrochemical loads was designed.

Use of Anode Baffle for Grid Control

All large pumped ignitron rectifiers employ a device which in principle is somewhat analogous to the grid in a multianode-type rectifier, but which usually has been called an anode baffle or shield when used in ignitrons. Figure 2 shows the cross section of a single-anode ignitron assembly. Please note the structure surrounding the anode which is called the deionizing anode baffle. This baffle, which is constructed of graphite and insulated from the metal vacuum-tight envelope, is energized through the grid seal. Its function in the ignitron-type rectifier has been to shield the main anode, thereby preventing or minimizing the occurrence of arc-backs. The fact that it was available, however, suggested that it might be used as another means of controlling the arc.

Factory tests were made to determine whether, with the right negative-bias voltage supplied to these grids, as they should be called when used for such purposes, sufficient control could be obtained to prevent current flowing from the anodes even though the ignitors were energized and firing. These tests indicated that there was enough grid action in the present-type ignitron-insulated anode baffle to prevent current flowing, when the baffles were biased negative with respect to the cathode by 300 volts, under expected conditions of operating temperature and positive main-anode voltages likely to be encountered in this field of application. Satisfactory blocking control could be obtained at this negative-bias level with as many as 500 ohms in series with the biasing circuit. This meant that a practical scheme for grid blocking the main arc in the rectifier could be developed.

Therefore, a cell-line starting circuit based on grid-blocking action in ignitron rectifiers was built. Figure 3 shows the schematic arrangement of a typical grid-bias starting circuit.

Procedure for Energizing Cell Line by Grid Blocking

A procedure for re-energizing a large ignitron rectifier installation after an outage using the Figure 3 circuit is as follows:

1. The 350-volt d-c bias rectifier is energized by closing the fused knife switch and contactor *A* in its supply circuit. In normal operation, the station operator energizes it by closing switch C_1 which is located on his master operating panel.

2. When operator's switch C_2 is closed, all *31S* relays close their contacts, and all the *31SX* relays open their contacts. This action then applies negative 350-volt d-c bias between the cathodes of all the rectifiers and the grids. It is now impossible for any of the rectifiers to carry current so long as this negative bias is maintained.

3. All power circuit breakers and disconnect switches are next closed connecting the rectifiers to both a-c supply voltage and the main d-c station bus.

4. The operator's master a-c excitation switch is then closed, which energizes all the individual firing circuits and causes all the ignitors to start firing. However, no current flows from the rectifiers, because the negative bias is maintaining a blocking action.

5. Finally, the short-circuiting switch *D* is closed, instantly removing the negative bias from all the grids in all the ignitron rectifiers. There remains the 110-volt positive a-c excitation from the baffle transformers, which had been canceled up to this time by the 350-volt negative bias. All 12 ignitron rectifiers, therefore, are permitted to carry current at the same instant.

6. Operator's switches C_1 and C_2 can now be opened. This de-energizes the 350-volt negative-biasing circuit, disconnecting it completely from the main power circuits. It also operates all relays *31S* and *31SX*, transferring the neutrals of the baffle transformers from the master biasing circuit back to the cathode terminals of the rectifiers.

Field Tests

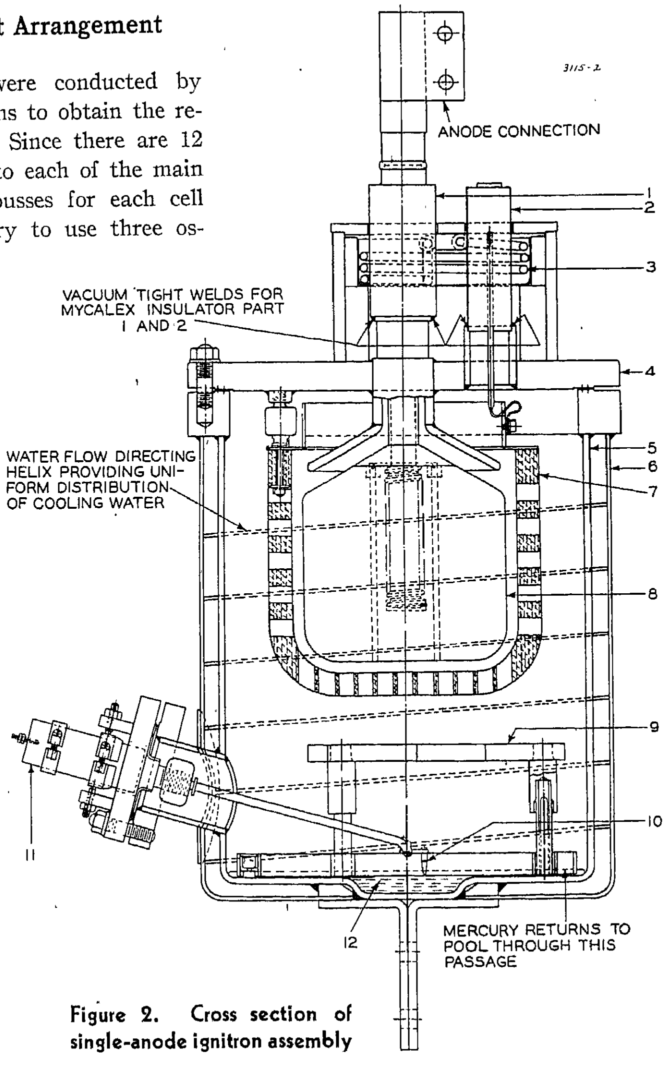
In order to make a thorough study of the problem, it was decided to make extensive tests in the field. Aluminum Company of America operates several reduction plants. Three western plants, all employing rectifiers, were selected. These plants will be designated as plants *A*, *B*, and *C*, for purposes of reference. Initial tests were made on one manufacturer's rectifiers at plant *A*. Confirming tests were made on rectifiers of other manufacture and type at plants *B* and *C* to make them fully comprehensive in nature.

Oscillographic-Test Arrangement

The field tests were conducted by means of oscillographs to obtain the required information. Since there are 12 rectifiers connected to each of the main 60,000-ampere d-c busses for each cell line, it was necessary to use three oscillographs in order to have available enough vibrators to record the behavior of the cathode currents in all 12 rectifiers. By using three oscillographs, 20 vibrators were available.

The three oscillographs were placed on a single operator's table. One vibrator in each oscillograph was connected to a common timing wave, so that the instant of starting the 12 rectifiers would be commonly indicated on the three oscillograms taken for each test. Then four supersensitive vibrators of oscillograph 1 were connected through shunt leads to the ammeter shunts in the cathode lines of rectifiers 1*A*, 1*B*, 2*A*, and 2*B*. Oscillograph 2 had four supersensitive vibrators connected to the four ammeter shunts in cathode lines of 3*A*, 3*B*, 4*A*, and 4*B*, and likewise oscillograph 3 was connected with ammeter shunts in cathode lines of 5*A*, 5*B*, 6*A*, and 6*B*. In each instance, main cell-line current was recorded on one of the films.

Control of the starting operation for all 12 rectifiers was transferred from the operator's main panel in the substation to control switches on the oscillographer's


3115-2
ANODE CONNECTION
1
2
3
VACUUM TIGHT WELDS FOR MYCALEX INSULATOR PART 1 AND 2
4
WATER FLOW DIRECTING HELIX PROVIDING UNIFORM DISTRIBUTION OF COOLING WATER
5
6
7
8
9
10
11
12
MERCURY RETURNS TO POOL THROUGH THIS PASSAGE

1. Mycalex anode insulator
2. Mycalex insulator (lead to insulated baffle)
3. Anode heater
4. Vacuum-chamber cover
5. Vacuum chamber
6. Water jacket
7. Energized (deionizing) anode baffle
8. Graphite anode
9. Mercury splash baffle
10. Ignitor tip
11. Mycalex insulator for ignitor and relieving anode leads
12. Mercury pool (cathode)

Figure 2. Cross section of single-anode ignitron assembly

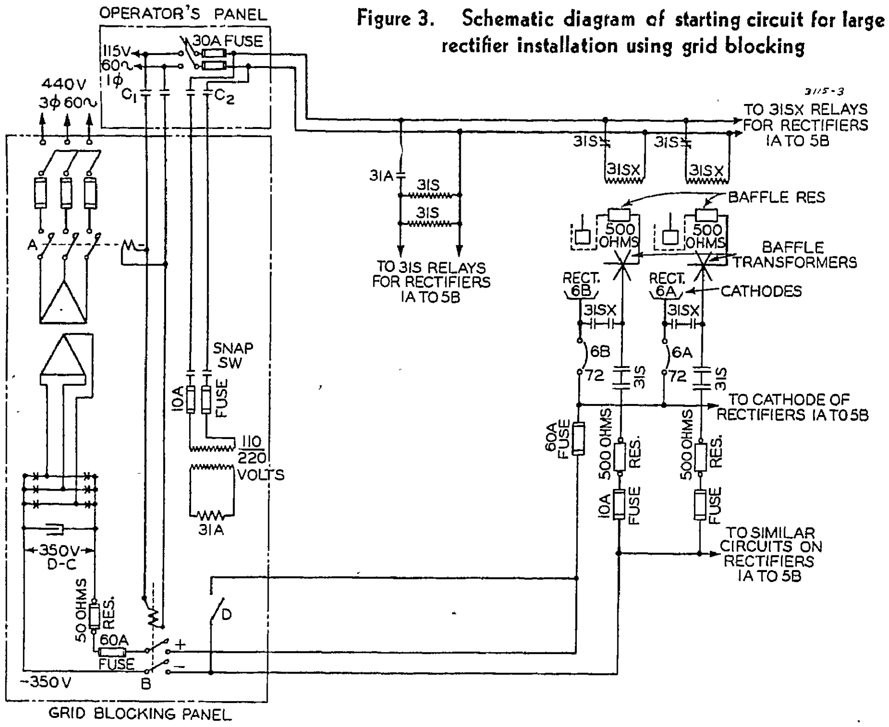


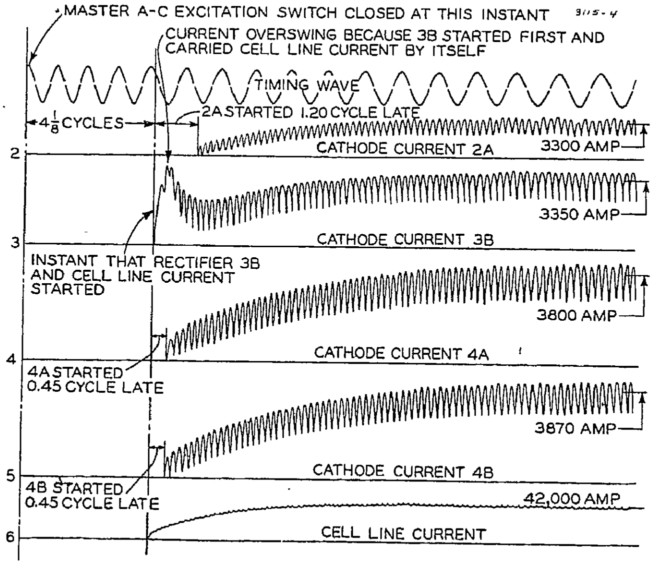
Figure 3. Schematic diagram of starting circuit for large rectifier installation using grid blocking

Figure 4 (below). Typical oscillogram starting cell line by master a-c excitation

On this test the oscillograms for the cathode currents of the other eight rectifiers showed the following results:

1*A* started 0.72 cycle late

1*B* started 0.60 cycle late



2*B* started 0.67 cycle late

3*A* started 0.64 cycle late

5*A* started 0.33 cycle late

5*B* started 0.33 cycle late

6*A* started 0.72 cycle late

6*B* started 0 cycle late same instant as 3*B*. Cathode current for 6*B* overswung same as 3*B*. Otherwise behavior of cathode current for these rectifiers was same as shown for 2*A*, 4*A*, and 4*B*

table. All 12 rectifiers would be first taken out of service simultaneously by the station operator. Then, after switching changes were completed quickly, the oscillographer, who controlled the single final starting operation, gave an audible signal, whereupon all three oscillographic instruments were started. The oscillographer, who gave the signal for starting the three oscillographic instruments, operated the master starting circuit for the 12 rectifiers a fraction of a second after giving the signal. This test setup and procedure worked remarkably well. The instruments were synchronized so accurately by their operators that the starting transients occurred on the oscillographic film for all tests less than 30 cycles after the starting signal. At the three plants a total of 13 such tests was made.

Results of Starting Tests Using Ignitor Control

Five tests were made at plant *A*, two at plant *B*, and one at plant *C* to record the behavior of the 12 separate rectifiers using the master a-c ignitor-excitation method. Figure 4 is typical of the oscillographic results of these tests. The number of cathode-current records shown in Figure 4 is limited to only 4 of the 12 rectifiers. This was necessary, because an illustration showing the behavior of all 12 rectifiers would be excessively large for publication. However, these four records typify the results.

The results of several of the starting tests using the master a-c ignitor-excitation method to demonstrate the effects or disparity in starting time of the individual rectifier units are given in Table I.

A special test was made at plant *B*, using the master a-c ignitor-excitation method to determine what effect cold ignitors and cathode mercury would have on starting. The volt-amperes output characteristic of the firing circuit used on these rectifiers was lower than on the rectifiers where the other starting tests with master a-c excitation were made. This volt-ampere characteristic was, however, sufficiently high to give satisfactory operation of the ignitors under continuous normal operating conditions after the ignitors and cathode mercury were warm. Results of this special test were that some of the rectifiers delayed as long as 20 to 30 cycles before the ignitors began to fire. Needless to say, with the

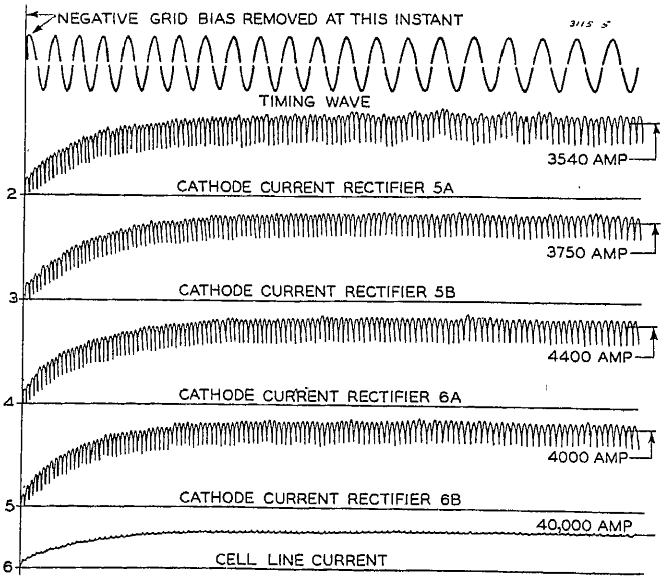


Figure 5. Typical oscillogram starting cell line by grid blocking

Cathode currents for rectifiers 1*A*, 1*B*, 2*A*, 2*B*, 3*A*, 3*B*, 4*A*, and 4*B*, started at same instant and had same appearance as cathode currents for 5*A*, 5*B*, 6*A*, and 6*B* shown on this oscillogram various individual rectifiers trying to pick up a cell line at various intervals over such a long period as 20 cycles, each of them tripped out on d-c overcurrent in turn as they started, and the rectifiers failed to energize the cell line.

Results of Starting Tests Using Grid Method

There were five cell-line starting tests made using the Figure 3 grid-blocking methods, two at plant *A* and three at plant *B*. The results of the tests are typically illustrated by Figure 5. For the same reasons as explained for Figure 4, only 4 records of cathode current are shown instead of 12.

During the tests with the grid blocking, the power circuits were all energized and the ignitors caused to fire for a short interval prior to the time when the negative grid bias was removed to pick up the cell-line load. There was no failure of blocking action at any time.

Conclusions

Based on the results of the various tests described, the following conclusions were reached:

1. While the cell-line current requires five to six cycles to reach its steady-state value, its initial rate of rise, while slow referred to the total load, is relatively high referred to the rating of a single rectifier. For this reason, unless all the rectifiers start carrying current at the same instant, when a cell line is energized, the first rectifiers to carry current accumulate load so rapidly that overload can exist in a cycle or less. The three-pole contactors used for the supplies to the individual ignitor-firing circuits were of a standard industrial-control type. A disparity as high as $2^{1}/_{8}$ cycles existed in the closing times of these contactors, as proved by the various starting tests. In view of the high rate of rise of cell-line current, if the conventional master a-c ignitor-excitation method of cell-line starting is to be used successfully (excluding effects of cold ignitor points and cold mercury), only contactors having a maximum difference in closing time not greater than one-half cycle should be used.

2. It was proved that the resistance characteristics of cold ignitors may be such that they require substantially higher volt-amperes to cause them to ignite cathode spots, when cold, after a service outage, than under normal operating conditions. Therefore, to insure satisfactory cell-line starting with ignitron rectifiers, the volt-ampere characteristics of the firing circuit must match these extra requirements if the master a-c excitation method is to be employed.

3. The new starting method for large rectifier installations employing the grid-blocking action available in ignitron rectifiers was demonstrated to be practical. The equipment used for making the starting tests by the new grid-blocking method was semipermanently connected in one of the ignitron-rectifier cell-line installations at plant *B*. At least six emergency outages have occurred subsequent to the tests described in the paper. In each instance the new grid-starting method was employed for re-energizing the cell line. All reports show that the scheme was completely successful in all cases. It is, therefore, believed that the grid-blocking method offers a better solution for starting large Ignitron installations of this sort. This conclusion may be reached, because the new method overcomes the difficulty with cold ignitors by permitting the station operators to run them for a sufficient period to warm them up and restore easy ignition characteristics; and it also eliminates the problem of obtaining control contactors having precision closing time, the outcome of which, at best, may be doubtful.

Addendum

During the tests, in addition to solving the problems outlined in the paper, several other interesting and valuable data were obtained. The effect of cell-line inductance in the operation of large electrochemical plants often has been speculated upon by engineers dealing with the problem. Records were obtained of the cell-line-current rate of rise and are shown in Figure 4 and Figure 5.

Cell-line inductance as measured by the starting transients in cell-line current, shown in Figure 4 and Figure 5, has been recognized as a factor affecting the performance of main cell-line breakers in the older installations using rotating apparatus for conversion purposes, and also the cathode breakers, which are used to trip the cell-line currents when shutting down installations employing mercury-arc rectifiers. While it is well known that large air circuit breakers trip with more uniform opening time than they close, nevertheless, the contacts are bound to start parting on some of the cathode breakers earlier than others during planned shutdowns.

Large electrochemical rectifier installations are shut down by operating a master switch which gives simultaneous tripping impulses to all the cathode breakers. When the faster cathode breakers start parting contacts, there is a rapid extinction of current from their associated rectifiers. The remaining rectifiers, whose cathode breakers are slower to open, therefore, accumulate the portion of the load dropped by the earlier rectifiers.

Two important effects, therefore, combine to impose substantially higher interrupting duty on the slower cathode breakers. First, overcurrent exists on one or more of the rectifiers. The magni-

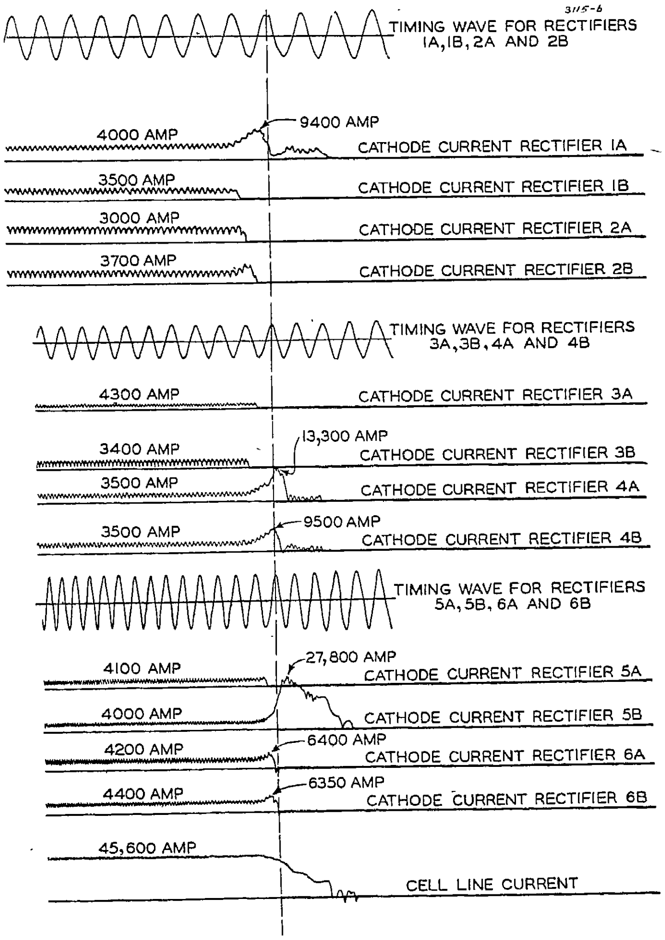


Figure 6. Typical oscillogram showing cathode current in all 12 rectifiers feeding one cell line and total cell-line current when cell line is de-energized by tripping cathode breakers by master tripping switch

The Quadrature Synchronous Reactance of Salient-Pole Synchronous Machines

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Synopsis: Advances in the theory of synchronous machines have created a number of reactance constants, and improvements in theory have led to continual refinements in methods of calculation and test. This paper presents an improvement in the calculation of the quadrature synchronous reactance, compares calculated with test results, and discusses four methods of test, one of which is an improvement in simplicity and ease of accomplishment over methods of test presented previously.

THE wide application of synchronous machines to many methods of power generation and utilization has caused the development and refinement of synchronous-machine theory as a tool to enable the accurate prediction of the behavior of these machines. This theory has created a number of characteristic constants, or coefficients, in terms of which the operation of the synchronous machine is defined. Papers dealing with one or more of these constants have appeared in the pages of the technical press from time to time, each paper broadening the general concepts or presenting methods of calculation or test which more sharply define particular constants.*

Past attention to individual reactance constants has been devoted chiefly to the armature leakage reactance[7] x_l, to the direct-axis quantities[2,6] x_d, x_d', x_d'', and to the negative- and zero-sequence reactances[2,6] x_2 and x_o. Less attention has been paid to the quadrature synchronous reactance[1,2] x_q either in the development of its full theory or in the presentation of test data. The situation is a natural one, as this reactance is not of major importance in either the short-circuit or starting characteristics of machines, and it is these characteristics which have received the most attention. Further, both the classic slip test[6] for x_q and reluctance power test[1] present difficulties in their execution which have discouraged the accumulation of data.

The quadrature synchronous reactance is of prime importance in determining the steady-state torque-angle characteristic which in turn evaluates the spring constant P_r. Accurate knowledge of either or both of these things is necessary when combining synchronous machines with mechanical drives having pulsating shaft torques such as are found in Diesel or gasoline engines and reciprocating compressors. The present tremendous increase in the use of Diesel and gasoline engines therefore makes an analysis of x_q particularly pertinent.

This paper presents refinements in the theory and calculation of x_q and advances two new methods of test for this quantity. It also presents a discussion of the two previously published test methods and compares the results which have been obtained by calculation and by the four test methods.

General Theory, Methods, and Test Results

The quadrature synchronous reactance x_q of salient-pole synchronous machines can be thought of as being made up of two major parts. One of the parts of this quantity is armature leakage reactance x_l the theory of which has been fully presented previously. The second part of this quantity is the reactance of armature reaction. This second quantity x_{aq} exists as a result of the generation of fundamental air-gap flux when armature current flows and the subsequent generation by that flux of a component of armature voltage which opposes an increase in the armature current. The magnitude of the flux and therefore the back voltage depend in part upon the permeance of the air gap opposite the armature magnetomotive-force wave.

In synchronous machines there are two axes of air-gap symmetry, the direct and quadrature axes, and hence two values of gap permeance. Each of these has been determined accurately over the range of variables encountered in modern machine design for unslotted air gaps, but the effect of slots in either or both the armature and field surfaces, while it has been recognized, has not been presented previously in the technical press. Usually the reactance of armature reaction constitutes the major part of x_q, and to predetermine this quantity accurately, its major component must be predetermined

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* See "References."

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tude of this current may reach as high as 600 per cent of the full-load current rating of an individual rectifier. Second, the high inductance of the cell-line load tends to maintain current through the breakers.

The opportunity was seized, during the tests described in the paper, to make some records of the behavior of current supplied by the various rectifiers to the d-c bus during cell-line shutdown. The results of these tests are shown in a typical fashion by Figure 6.

Rectifier cathode-current traces in Figures 4, 5, and 6 indicate a high 360-cycle d-c current ripple. Actually, this ripple is substantially lower. It was necessary to use standard d-c shunts instead of oscillograph shunts. With standard shunts the effect of shunt inductance, as well as the effect of fields on shunt leads, is responsible for this apparently large ripple. This must be taken into account in analyzing the oscillograms.

References

1. ALCOA RECTIFIER INSTALLATION, **J. Elmer Housley, H. Winograd.** AIEE TRANSACTIONS, volume 60, 1941, pages 1266–74.

2. ELECTRICAL EQUIPMENT FOR LARGE ELECTROCHEMICAL INSTALLATIONS, **T. R. Rhea, H. H. Zielinski.** AIEE TRANSACTIONS, volume 61, 1942, October section, pages 733–41.

3. IGNITRON RECTIFIERS IN INDUSTRY, **J. H. Cox, G. F. Jones.** AIEE TRANSACTIONS, volume 61, 1942, October section, pages 713–18.

4. POWER RECTIFIERS IN ALUMINUM PRODUCTION, **J. H. Cox, D. I. Bohn.** *Chemical and Metallurgical Engineering*, September 1941.

5. THE ALUMINUM INDUSTRY, **J. D. Edwards, F. C. Frary, Zay Jeffries.** McGraw-Hill Book Company, Inc., 1930. Volume I, pages 319–21.

6. THE 45,000-KW SYNCHRONOUS-CONVERTER SUBSTATION OF THE ALUMINUM COMPANY OF AMERICA AT MASSENA SPRINGS, N. Y., **J. L. Burnham, R. C. Muir.** *General Electric Review*, September 1915.

7. CONVERTER INSTALLATION IN THE LAUTA ALUMINUM WORKS (in German), **W. Heins.** *AEG Mitteilungen* 3, volume 18, 1922, pages 75–82b.

8. IGNITOR EXCITATION CIRCUITS AND MISFIRE INDICATION CIRCUITS, **A. H. Mittag, A. Schmidt, Jr.** AIEE TRANSACTIONS, volume 61, 1942, August section, pages 575–7.

9. EXCITATION CIRCUITS FOR IGNITRON RECTIFIERS, **H. C. Myers, J. H. Cox.** AIEE TRANSACTIONS, volume 60, 1941, October section, pages 943–8

accurately. In Appendix B a method of calculating the quadrature reactance of armature reaction is presented. This method is based on obtaining the unslotted quadrature-axis air-gap permeance in the conventional manner and then applying to it selected fringing coefficients to account for the slots in the magnetic surfaces. After a value for the reactance of armature reaction has been obtained, the armature leakage reactance is added to it to obtain x_q. The results obtained by calculation are substantiated, as shown in Table I, by several different methods of test.

Unlike the method used for calculating x_q, the test for it must measure the entire quantity, both the armature leakage reactance and the reactance of armature reaction, as it is impossible in test to separate x_q into its analytical components and measure each individually. This restriction places on all test methods the necessity of measuring terminal conditions at an instant when these conditions can be used to evaluate x_q. This instant always occurs just prior to a state of instability, and consequently accurate measurements are not easily made.

In the first method of test, the slip test of Appendix C, an oscillograph is used to record the terminal voltage and armature current as the rotor is forced to slip past the axis of armature magnetomotive force. Unfortunately, the very slip which makes the test possible introduces its own errors. If the slip is large, over $1^1/_2$ per cent, currents are often induced in the usually present amortisseur windings, and the armature current is influenced by them, so that it may no longer reflect the true rotor permeance. While smaller values of slip can be obtained, they require accurate control of the terminal voltage and ability to adjust it, usually over the range from 20 per cent to 30 per cent of normal voltage. This control and adjustment is seldom available anywhere except on the manufacturer's premises, and it is not always easy to obtain it there for every machine.

The reluctance-torque method, described in Appendix D, is based on the determination of the maximum power the machine will carry when operating as a reluctance motor. The motor, with field open-circuited, can be pulled out of step either by increasing the shaft load with terminal voltage held constant or by reducing the terminal voltage with the shaft load held constant. Since the measurements are always made at the moment of instability, it is important in both of the preceding methods that the shaft load contain no torque pulsations. If the adjustable-shaft-load method is used, it is important that the load be capable of fine adjustment, so that the approach to the point of instability can be made accurately. If the test is taken at no load, the line voltage is reduced until the machine falls out of step because of the friction and windage load. This requires the same type of terminal voltage control necessary in the slip test and hence presents the same difficulties. In addition, it also requires accurate measurement of the friction and windage load.

Table I. Quadrature Synchronous Reactance x_q by Calculation and Test

					x_q				
						Tests			
Type of Machine	Kilovolt-amperes	Volts	Frequency (Cycles Per Second)	Rpm	Calculation	Slip	Reluctance Torque	Negative Excitation	Maximum Lagging Current
Motor	1,405	4,160	60	1,800	0.98	1.03	0.98	1.00	0.96
Motor	300	440	60	1,200	1.18		0.97	1.26	1.16
Motor	380	440	60	1,200	1.14	1.09	1.11	1.10	1.14
Industrial-type generator	31.3	208	60	900	0.43	0.42	0.41	0.39	0.43
Motor	900	4,150	60	900	0.98	0.91	1.03	0.98	0.98
Motor	2,700	6,600	60	900	0.98	1.04	1.02	1.00	1.00
Motor	1,160	2,200	60	900	0.74	0.77		0.77	0.72
Motor	1,250	6,600	60	900	0.53	0.54	0.59	0.58	0.57
Motor	1,358	4,100	60	900	0.83	0.83	0.80	0.82	0.82
Motor	2,200	2,200	60	720	0.72	0.71	0.68	0.70	0.70
Motor	1,175	2,200	60	600	0.75	0.77	0.83	0.81	0.82
Motor	5,900	6,600	60	514	1.02	0.96	1.02	0.97	0.98
Motor	2,100	6,600	25	500	0.92	0.92		0.92	0.93
Motor	1,465	2,200	60	450	0.82	0.82		0.84	0.82
Motor	690	4,000	60	400	0.84	0.86	0.85	0.87	0.85
Motor	7,320	6,900	60	400	0.43	0.43			
Industrial-type generator	187	400	50	375	0.53	0.63	0.57	0.63	0.66
Industrial-type generator	500	4,160	60	360	0.60	0.58	0.59	0.57	
Industrial-type generator	625	4,160	60	360	0.65	0.67	0.65	0.67	0.66
Industrial-type generator	375	2,400	60	327	0.75	0.73	0.71		
Motor	205	2,200	60	300	0.99	1.00	0.89	0.95	0.93
Motor	1,380	4,000	60	277	0.77			0.70	0.74
Waterwheel-type generator	6,250	4,150	60	240	0.55	0.57	0.59		
Motor	780	2,300	60	164	1.08	1.09	0.98	1.10	1.08
Waterwheel-type generator	24,000	13,200	60	164	0.58	0.58			0.59

The negative-excitation test, described in Appendix E, is based on the determination of the amount of field excitation required to overcome the reluctance torque of the machine. The test is taken with the machine operating as a synchronous motor with a free shaft and at rated line voltage, thus eliminating the necessity for a variable voltage supply. It is important in this test that the shaft load be free of torque harmonics and of as small a magnitude as it is possible to obtain. As this small load determines the point of instability, it is important that it be known to calculate x_q.

The fourth and most satisfactory test method, the maximum-lagging-current test, described in Appendix F, uses the same method of test as in the negative-excitation method of Appendix E. In this case it is not necessary to measure shaft power, other than to be sure that it is small, as line voltage and current readings are sufficient. Because the line current is large in size and easy to measure, the test is simpler in its technique than previous methods. Further, a variable line voltage is not required, because, like the negative-excitation test, results can be obtained at rated voltage. The value of x_q is simply the ratio of voltage and maximum stable current.

Thus, in the course of developing various methods of test, the objections have one by one been removed. The necessity of having an oscillograph was first removed, then the requirement of a controlled low-voltage source, and finally the low power readings. The results obtained by the various methods of test are shown in Table I. It was not possible to test each machine included in the table by every test method, and the blanks indicate lack of test data on the particular machine. The agreement is, in general, excellent. No attempt has been made to establish divergences between test and calculation or between test methods. These are apparent from a study of the table itself.

Conclusions

The paper has presented improvements in the method of calculating x_q which enable the accurate prediction of this quantity. It has reviewed two well-known test methods and presented two new ones, one of which, the maximum-lagging-current test, has great advantages in simplicity of technique and ease of accomplishment. These advantages should lead to a wide acceptance of the maximum lagging-current test as a recognized test procedure.

Appendix A. Nomenclature

Electrical quantities, voltage, current, power, reactance, and resistance, are in the unit system and are represented by symbols which have already been generally recognized. Other quantities are, when necessary, defined adjacent to their initial use in the text, and all definitions are consolidated in this appendix.

A = square wave value of armature-reaction ampere turns per pole

$$A_1 = \frac{\text{maximum ordinate of fundamental flux-density wave}}{\text{maximum ordinate of actual flux-density wave}}$$

in the air gap of a synchronous machine when excited only from the rotor (saturation is neglected)

$$A_{q1} = \frac{\text{maximum ordinate of fundamental flux-density wave}}{\text{maximum ordinate of actual flux-density wave that would be produced by the same magnetomotive force acting on a uniform gap equal to } g_{\min}}$$

in the air gap of a synchronous machine when excited by sine wave of armature magnetomotive force in the quadrature axis (saturation is neglected)

C_{0fdf} = total fringing coefficient applied to g'_{fdf} to obtain g_{fdf}

C_{0fqa} = total fringing coefficient applied to g'_{fqa} to obtain g_{fqa}

e = per unit armature terminal voltage

E = particular value of per unit armature terminal voltage

e_d = per unit direct-axis field excitation, neglecting saturation

E_d = particular value of per unit direct-axis field excitation, neglecting saturation

F = field ampere turns in the air gap required to produce normal voltage in the armature on open circuit

$g_{\min}$ = iron-to-iron air gap at the pole center when opposite an armature tooth

g'_{fdf} = apparent air gap seen by the maximum ordinate of the fundamental direct-axis field flux-density wave, neglecting fringing phenomena

g_{fdf} = air gap seen by the maximum ordinate of the fundamental direct-axis field flux-density wave including fringing phenomena

g'_{fqa} = apparent air gap seen by the maximum ordinate of the fundamental quadrature-axis armature flux-density wave, neglecting fringing phenomena

g_{fqa} = air gap seen by the maximum ordinate of the fundamental quadrature-axis armature flux-density wave, including fringing phenomena

I_1 = particular value of per unit line current

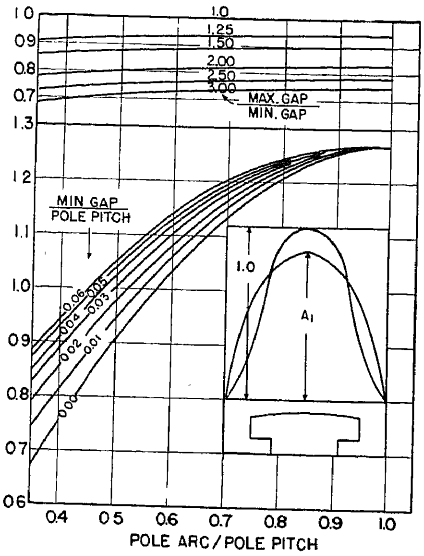

Figure 1. Ratio of the maximum amplitudes of the fundamental component and the actual flux-density waves in the air gap of a synchronous machine excited only from the rotor

Maximum value of actual flux-density wave = unity

Maximum value of fundamental flux-density wave, $A_1 = A \times B$

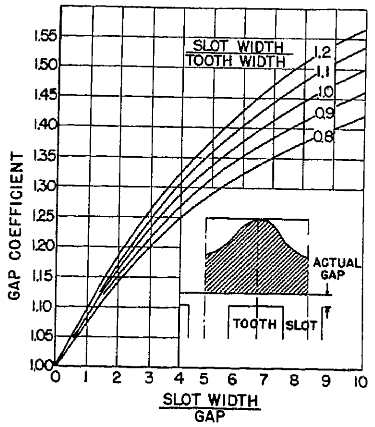

Figure 2. Air-gap fringing coefficient for the flux around a tooth of finite width and a slot of infinite depth as obtained by elliptic function analysis

Use the magnetic gap in place of the actual gap when the two differ

P = per unit value of shaft power (unit power is numerically equal to base kilovolt-amperes)

P_1 = particular value of per unit shaft power

P_r = synchronizing coefficient determined by dividing the shaft power in kilowatts by the corresponding angular displacement in electrical radians of the rotor from its zero shaft power position

Q = per unit value of reactive power (Unit reactive power is base kilovolt-amperes)

Q_1 = particular value of per unit reactive power

r_a = per unit armature resistance

x_l = per unit armature leakage reactance

x_d = per unit direct-axis synchronous reactance

x_q = per unit quadrature-axis synchronous reactance

x_{aq} = per unit quadrature-axis reactance of armature reaction

x = per unit external reactance

δ = load angle in electrical degrees

Γ, Υ, Ω = quantities defined in equations 20, 22 and 25, respectively

Appendix B. The Calculation of x_q

The quadrature-axis synchronous reactance x_q is defined as the ratio of the fundamental component of reactive armature voltage, due to the fundamental quadrature-axis component of armature current to this component of current under steady-state conditions and at rated frequency. Usually the reactance is taken as the value corresponding to rated armature current.

Alger[7] divided this quantity into two parts, the reactance of armature reaction

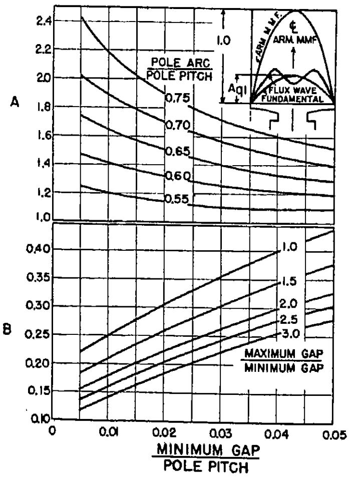

Figure 3. Fundamental of the air-gap flux-density wave when a salient-pole synchronous machine is excited by only a sine-wav armature magnetomotive force whose axis is in quadrature with the pole center

Maximum value of armature magnetomotive force = unity. Fundamental $A_{q1} = A \times B$

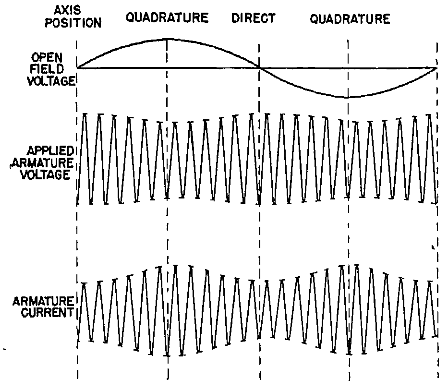

Figure 4. Appearance of oscillogram, slip test for x_q

In practice slip must be the minimum possible and only a small fraction of that illustrated

$$x_q = \frac{\text{per unit applied armature voltage}}{\text{per unit armature current}}$$

where both values are read from the quadrature-axis position

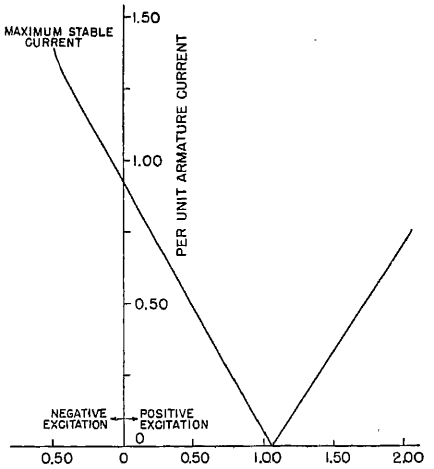

Figure 5. Per unit armature current versus excitation at constant terminal voltage

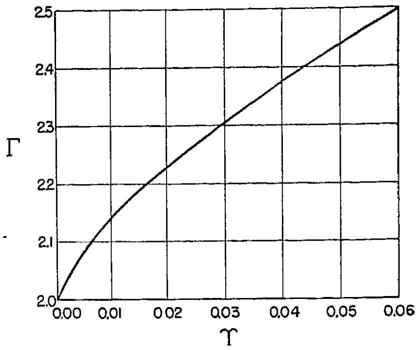

Figure 6. Plot of equation 22

x_{aq}, and the armature leakage reactance x_l, and produced methods for the calculation of x_l. Wieseman,[9] by means of curves derived from flux plots, evaluated coefficients A_1 and A_{q1} which, for smooth air gaps, made possible the determination of the ratio of fundamental air-gap flux due to field magnetomotive force. Linville[4] showed how to use these coefficients to calculate x_{aq}. Both Wieseman's presentation and Linville's employment of it were, as noted, based on smooth gaps. Kilgore[2] pointed out that an "effective" gap, not the actual measured gap at the pole center, must be used when slotted surfaces are considered, but a method of obtaining that effective gap and the employment of the result has not been presented. This, a step needed for the accurate calculation of x_q, is presented here.

The problem can be approached most clearly by first considering the phenomena existing in the direct-axis air gap when the machine is excited only by the field winding. Next, the effect of quadrature excitation from the armature will be introduced, and finally the two effects will be considered jointly to produce an equation for x_{aq}.

Assume, for the moment, that sufficient field excitation is applied to a smooth air gap to produce an actual flux-density wave whose maximum ordinate, at the pole center, is unity. Then from Figure 1, the amplitude of the maximum ordinate of the fundamental of that wave is A_1. The air gap seen by the maximum ordinate of the actual flux-density wave is the iron-to-iron gap at the pole center g_{min}. Since both the actual and fundamental flux-density waves are created by the same magnetomotive force, their different amplitudes indicate that the magnetic gaps seen by the actual and fundamental components are different. Since the ratio of the amplitude of the flux-density waves is A_1, and the gap seen by the maximum ordinate of the actual wave is g_{min}, then the gap seen by the maximum ordinate of the fundamental wave is

$$g'_{fdf} = \frac{g_{min}}{A_1} \tag{1}$$

Carter has derived the relationship between the magnetic gaps of two parallel smooth surfaces and one smooth and one slotted surface. His fringing coefficients, which also can be thought of as "equivalent gap ratios" are well known. Figure 2 shows this coefficient for stator slots over a range of parameters encountered in synchronous machines. Similar characteristics can be obtained for stator air ducts, rotor slots, and other discontinuities in the magnetic surfaces. When these fringing coefficients are applied to the fundamental gap, g'_{fdf}, the relationship between the measured iron-to-iron minimum gap and the magnetic minimum gap for the fundamental component of the actual flux wave can be approximated closely.

To use this approximation, the actual iron-to-iron minimum gap, g_{min}, is the starting point. g'_{fdf}, the effective smooth gap for the direct-axis fundamental flux-density wave due to rotor excitation, is next found from equation 1. Then using g'_{fdf}, the fringing coefficient for the armature slots is found from Figure 2, and similarly, fringing coefficients for other magnetic discontinuities in the air gap are obtained. If the product of the several fringing coefficients for this condition is $C_{0_{fdf}}$, then the magnetic gap seen by the fundamental component of the direct-axis field-excited flux-density wave is

$$\begin{aligned} g_{fdf} &= C_{0_{fdf}}\, g'_{fdf} \\ &= C_{0_{fdf}} \frac{g_{min}}{A_1} \end{aligned} \tag{2}$$

Figure 3 shows the ratio between the maximum per unit amplitudes of the fundamental component of the flux-density wave and the fundamental magnetomotive-force wave in the quadrature axis when the machine is excited only by a sine wave of armature magnetomotive force in the quadrature axis. If this identical unit magnetomotive-force wave that is applied to the complex smooth gap configuration of Figure 3 were applied to a constant-length smooth gap equal to g_{min}, then the fundamental component of the flux-density wave would be unity. Therefore, the ratio A_{q1} is not only a ratio between a magnetomotive-force wave and a flux-density wave, but may also be thought of as the ratio between the fundamental component of quadrature-axis armature flux and unit fundamental armature flux, both of which are created by unit fundamental armature magnetomotive force. Hence, like A_1, it also is an inverse ratio of magnetic gap and the gap seen by the maximum ordinate of the fundamental quadrature-axis armature flux is

$$g'_{fqa} = \frac{g_{min}}{A_{q1}} \tag{3}$$

If similar approximations are applied to g'_{fqa} as were applied to g'_{fdf} to get the appropriate fringing coefficients, the magnetic gap seen by the fundamental component of quadrature axis armature flux is

$$\begin{aligned} g_{fqa} &= C_{0_{fqa}}\, g'_{fqa} \\ &= C_{0_{fqa}} \frac{g_{min}}{A_{q1}} \end{aligned} \tag{4}$$

Under the assumption of smooth air-gap surfaces and with the application of identical fringing coefficients to all methods of excitation, the relationships determining x_{aq} have been shown to be[4]

$$x_{aq} = \frac{4}{\pi}\,\frac{A}{F}\,\frac{A_{q1}}{A_1} \tag{5}$$

When the fringing coefficients are recognized to be different and dependent upon the method of excitation, as set forth here, this equation becomes

$$x_{aq} = \frac{4}{\pi}\,\frac{A}{F}\,\frac{A_{q1}}{A_1}\,\frac{C_{0_{fdf}}}{C_{0_{fqa}}} \tag{6}$$

Also there is the relationship

$$x_q = x_l + x_{aq} \tag{7}$$

Therefore

$$x_q = x_l + \frac{4}{\pi}\,\frac{A}{F}\,\frac{A_{q1}}{A_1}\,\frac{C_{0_{fdf}}}{C_{0_{fqa}}} \tag{8}$$

Appendix C. The Slip Test For x_q

The slip test for x_q has already been presented adequately in the technical press,[6] and so only a brief description is necessary. The machine to be tested is either coupled to a second machine or run with a free shaft and positive-sequence voltage applied to the terminals. The field winding is open-circuited. The terminal voltage or shaft torque is then adjusted so that the reluctance torque of the machine is overcome, causing the field to slip past the armature magnetomotive-force wave at a low value of slip. An oscillographic record of the armature current, terminal voltage, and induced field voltage, is then taken. Figure 4 illustrates the appearance of such an oscillogram.

The quadrature synchronous reactance equals the minimum ratio of the per unit armature voltage and current read from the oscillogram. The correct values of armature voltage and current are obtained at those instances when the induced field voltage is at a positive or negative maximum.

The slip method of measurement, using an oscillograph, has the advantage of measuring most clearly those quantities on which the definition of x_q is based and of providing a permanent record of the quantities read. It also has disadvantages. Since the terminal voltage applied must usually be approximately 20 per cent to 30 per cent of the rated voltage and must be adjusted accurately, a controllable low-voltage power supply is required. It is important that the slip be very small; otherwise in machines having amortisseur windings, currents will be induced in these windings, and the reactances read from the oscillogram will be low. Even under the most strictly controlled conditions the instantaneous slip of the rotor is usually a minimum when the stator magnetomotive force is passing through the quadrature axis, and hence a difference between the accuracies of x_d and x_q, as read by the oscillogram, is inherent in the method.

For these reasons the slip method is generally limited to testing in the manufacturer's plant and is not broadly suitable for field work.

Appendix D. Reluctance Torque Test For x_q

Kingsley[1] first suggested the reluctance-torque test for x_q. Neglecting losses, the power-angle equation of an ideal synchronous machine connected to an infinite bus is given by[10]

$$P=\frac{ee_d}{x_d}\sin\delta+\frac{e^2(x_d-x_q)}{2x_dx_q}\sin 2\delta \qquad (9)$$

If the machine is operated as a reluctance motor, $e_d=0$, and maximum synchronous power will occur when $\sin 2\delta=1.0$ (that is, $\delta=45$ degrees). Any change tending to increase δ will cause loss of synchronism.

By lowering the terminal voltage or increasing the shaft load, the machine may be made to pull out of synchronism. Knowing x_d from other tests, and determining E and P_1 at the point at which loss of synchronism occurs, x_q can be found from the relation

$$x_q=\frac{E^2x_d}{E^2+2P_1x_d} \qquad (10)$$

It can be shown that the effect of armature resistance is approximately taken into account by the equation

$$x_q=\frac{E^2(x_d+2r_a)}{E^2+2P_1x_d} \qquad (11)$$

If P_1 is only the friction and windage of the machine, the drop-out voltage will be low (15 per cent to 25 per cent of normal), saturation will not be present, and core losses will be small.

Errors in the measurement of the power, and particularly the pull-out voltage, are possible as the machine is unstable as this condition is approached. The magnitude of error in determining x_q can be found from the following equation, which is derived from equation 10:

$$\frac{dx_q}{x_q}=\left(1-\frac{x_q}{x_d}\right)\left(2\frac{de}{e}-\frac{dP}{P}\right) \qquad (12)$$

Reactance external to the test machine causes pull-out at some angle less than 45 degrees, and the calculated value of x_q is too high.

If x is the external reactance, the load angle at pull-out is found to be

$$\delta=\text{arc tan}\frac{x_q}{x_d}\frac{(x_d+x)}{(x_q+x)} \qquad (13)$$

The value of x_q is then given by

$$x_q=\frac{E^2(x_d\sin 2\delta+2r_a)}{E^2\sin 2\delta+2P_1x_d} \qquad (14)$$

If the effects of armature resistance and external reactance are neglected, the error is given, very closely, by the relation

$$\frac{\Delta x_q}{x_q}\cong\left(1-\frac{x_q}{x_d}\right)(1-\sin 2\delta) \qquad (15)$$

The reluctance-torque test for x_q has one advantage over the slip test, namely, that satisfactory readings are possible without the use of an oscillograph. It has many of the disadvantages of the slip test. Again a controllable low-voltage source is usually required, and tests are generally possible only on the manufacturer's premises. In addition, shaft power must be known accurately.

Appendix E. Negative-Excitation Test for x_q

The negative-excitation test for x_q derives its name from the reversed polarity which is imposed on the field winding when the instruments are read and the necessity of recording this value. The machine to be tested is run as a synchronous motor, with a free shaft and with constant applied rated terminal voltage. The field current is initially adjusted to its value at the low point in the V-curve characteristic. It is then decreased to zero, causing an increase in line current, and, after reaching zero field current, the polarity of the applied excitation is reversed and field current increased in the negative direction, causing a continuing increase in the armature current. See Figure 5. By applying small increments in field current as the maximum stable negative field current is approached, the highest per unit value E_d of field current that can be obtained is found. Per unit shaft power, P_1, at this point is also determined. If the per unit direct-axis synchronous reactance x_d is known from other tests, x_q can then be found from the two readings taken.

Neglecting losses and applying negative

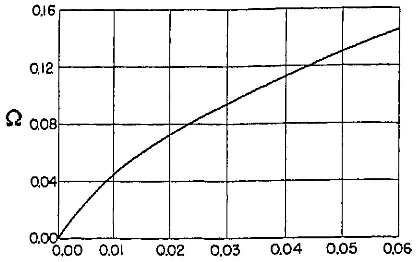

Figure 7. Plot of error function Ω

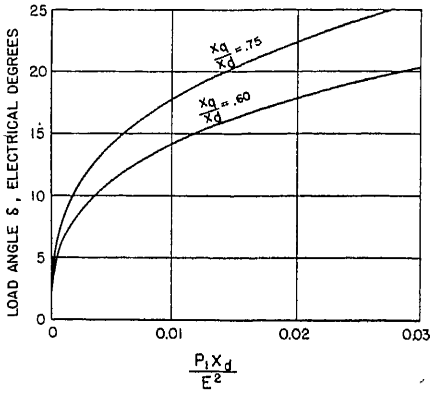

Figure 8. Plot of equation 31

excitation, the power-angle equation 9 becomes

$$P=\frac{e(-e_d)}{x_d}\sin\delta+\frac{e^2(x_d-x_q)}{2x_dx_q}\sin 2\delta \qquad (16)$$

When the machine is operating at its maximum stable point, there is

$$\frac{dP}{d\delta}=0 \qquad (17)$$

and

$$\frac{ee_d}{x_d}\cos\delta=\frac{e^2(x_d-x_q)}{x_dx_q}\cos 2\delta \qquad (18)$$

from which

$$\delta=\text{arc cos}\frac{1.0+\sqrt{1+2\Gamma^2}}{2\Gamma} \qquad (19)$$

where

$$\Gamma=\frac{2e}{e_d}\left(\frac{x_d}{x_q}-1.0\right) \qquad (20)$$

Combining and simplifying equations 16, 19 and 20 there results

$$P=\frac{ee_d}{x_d}\Upsilon \qquad (21)$$

where

$$\Upsilon=\sqrt{\frac{(\sqrt{1.0+2\Gamma^2}-3.0)^3}{32(\sqrt{1.0+2\Gamma^2}-1.0)}} \qquad (22)$$

Equation 22 is plotted in Figure 6.

If test values of $P(P=P_1)$, $e_d(e_d=E_d)$, $e(e=E)$, and x_d are substituted in equation 21, there is

$$\Upsilon=\frac{P_1x_d}{EE_d}$$

and the quantity Υ can be evaluated by test. Figure 6 can then be used to evaluate Γ, and from equation 20

$$x_q=\frac{x_d}{\frac{\Gamma E_d}{2E}+1.0} \qquad (23)$$

Test errors may occur in the determination of E_d and P_1. It can be shown that, if such errors do occur, the effect on x_q can be evaluated from the relationship

$$\frac{dx_q}{x_q}=-\left(1.0-\frac{x_q}{x_d}\right)\left(\Omega\frac{dP_1}{P_1}+(1.0-\Omega)\frac{dE_d}{E_d}\right) \qquad (24)$$

where

$$\Omega=\frac{\sqrt{1.0+2\Gamma^2}-3.0}{\sqrt{1.0+2\Gamma^2}+1.0} \qquad (25)$$

The error function Ω is plotted against Υ in Figure 7 for the range of Υ usually encountered. A study of this reveals that the value of x_q so determined is more affected by a test error in E_d than by a corresponding one in P_1.

The negative-excitation test for x_q, has an important advantage over both the slip and reluctance-torque methods of test in that a controllable low-line-voltage source is not required. Tests can be made on the user's premises where rated line voltage is frequently the only source of power, but, as in the reluctance-torque test, shaft power must be known, and this introduces the problem of measuring it.

Appendix F. Maximum-Lagging-Current Test for x_q

The maximum-lagging-current test for x_q is made in exactly the same manner as the negative-excitation test discussed in Appendix E. With the machine operating with a free shaft and with constant terminal voltage applied, the maximum stable per unit line current I_1, corresponding to the maximum stable negative excitation E_d, is found. x_q can then be found from these line-voltage and line-current values.

Neglecting losses and applying negative excitation as in Appendix E, the magnitude of negative excitation at the maximum stable operating point is, by solving equation 18, found to be

$$e_d=\frac{e(x_d-x_q)}{x_q}\,\frac{\cos 2\delta}{\cos\delta} \qquad (26)$$

The general reactive-power-angle equation of a synchronous machine connected to an infinite bus is

$$Q=\frac{ee_d}{x_d}\cos\delta-\frac{e^2}{x_d}\cos^2\delta-\frac{e^2}{x_q}\sin^2\delta \qquad (27)$$

When negative excitation, as given in equation 26, is applied to equation 27, the maximum stable reactive power is

$$Q_1=-\frac{e^2}{x_q}\left[1-\left(1-\frac{x_q}{x_d}\right)\sin^2\delta\right] \qquad (28)$$

In an ideal machine operating at no load with no losses, the load angle δ and the active power P are both zero when maximum reactive power is reached.

For these conditions

$$Q_1=EI_1=-\frac{E^2}{x_q} \qquad (29)$$

and in magnitude

$$x_q=\frac{E}{I_1} \qquad (30)$$

In an actual machine, power must be developed to overcome friction and windage, and the load angle at the maximum stable point cannot be zero. However, neglecting armature losses, it can be shown as follows that for the usual range of machine constants and friction and windage losses, these effects produce negligible errors in equation 30.

If equation 26 is substituted in equation 9, the power P_1 with negative excitation and at the maximum stable point is

$$P_1=\frac{E^2(x_d-x_q)}{x_dx_q}\sin^2\delta\tan\delta \qquad (31)$$

Figure 8 is a plot of equation 31 showing the relationship between δ and P_1x_d/E^2 over a range of machine constants. It can be seen from Figure 8 that at normal voltage and with usual values of x_d and P_1, δ may be as large as 20 degrees but probably will not exceed 15 degrees. If δ=20 degrees, and $x_q/x_d=0.6$, equation 28 gives a value for Q_1 which is 95.7 per cent of the maximum possible value given by equation 29. Hence, x_q, as determined by equation 30, would be approximately five per cent high. The inclusion of armature losses will decrease this figure slightly. It is felt that this figure of five per cent is a reasonable maximum variation between x_q determined by this method and the correct value and that in most cases the error will be less than five per cent. Table I justifies this conclusion.

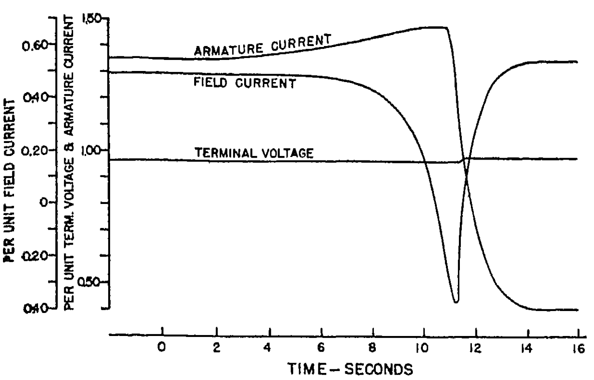

Figure 9. Terminal characteristics during a negative-excitation slip cycle

The terminal characteristics during a typical negative-excitation slip cycle have been recorded on oscillograms. Figure 9 shows the envelopes of the terminal voltage and armature current and a trace of the field current during one of these cycles. Previous to time $t=0$, the machine was operating at the maximum-stable-armature-current point of Figure 5. At $t=0$ the field voltage was slightly increased, and the machine became unstable and started to slip. As the slip increased, the armature current increased, reaching a maximum unstable value at $t=11$ seconds. As the unstable region was passed, the armature current fell until at $t=16$ seconds it was operating stably on the positive excitation side of the V curve of Figure 5. This phenomenon of rise and fall in current, when passing through the slip cycle, is readily apparent on test meters. The maximum stable current, not the maximum current during slipping, is used to determine x_q.

The maximum-lagging-current test for x_q has a great advantage over all other test methods in that the instruments required are perfectly standard and widely available. The test is made at rated voltage, and shaft power is not required to calculate the results. It is by far the simplest of the methods presented.

References

1. MEASUREMENT OF QUADRATURE-AXIS SYNCHRONOUS REACTANCE (letter to the editor), **Charles Kingsley.** AIEE TRANSACTIONS, volume 54, 1935, page 572.

2. CALCULATION OF SYNCHRONOUS MACHINE CONSTANTS, **L. A. Kilgore.** AIEE TRANSACTIONS, volume 50, 1931, page 1201.

3. DETERMINATION OF SYNCHRONOUS-MACHINE CONSTANTS BY TEST, **S. H. Wright.** AIEE TRANSACTIONS, volume 50, 1931, pages 1331–50.

4. STARTING PERFORMANCE OF SALIENT-POLE SYNCHRONOUS MOTORS, **T. M. Linville.** AIEE TRANSACTIONS, volume 49, 1930, page 531.

5. DEFINITION OF AN IDEAL SYNCHRONOUS MACHINE AND FORMULA FOR THE ARMATURE FLUX LINKAGES, **R. H. Park.** *General Electric Review*, volume 31, 1928, page 332.

6. THE REACTANCE OF SYNCHRONOUS MACHINES, **R. H. Park, B. L. Robertson.** AIEE TRANSACTIONS, volume 47, 1928, page 514.

7. CALCULATION OF THE ARMATURE REACTANCE OF SYNCHRONOUS MACHINES, **P. L. Alger.** AIEE TRANSACTIONS, volume 47, 1928, page 493.

8. SYNCHRONOUS MACHINES—III. TORQUE-ANGLE CHARACTERISTICS UNDER TRANSIENT CONDITIONS, **R. E. Doherty, C. A. Nickle.** AIEE TRANSACTIONS, volume 46, 1927, pages 1–14.

9. GRAPHICAL DETERMINATION OF MAGNETIC FIELDS: PRACTICAL APPLICATIONS TO SALIENT-POLE SYNCHRONOUS MACHINE DESIGN, **R. W. Wieseman.** AIEE TRANSACTIONS, volume 46, 1927, page 141.

10. SYNCHRONOUS MACHINES—I. AN EXTENSION OF BLONDEL'S TWO-REACTION THEORY, **R. E. Doherty, C. A. Nickle.** AIEE TRANSACTIONS, volume 45, 1926, page 912.

11. SYNCHRONOUS MACHINES—II. STEADY-STATE POWER-ANGLE CHARACTERISTICS, **C. A. Nickle, R. E. Doherty.** AIEE TRANSACTIONS, volume 45, 1926, pages 927–42.

12. REACTANCE OF SYNCHRONOUS MACHINES AND ITS APPLICATIONS, **R. E. Doherty, O. E. Shirley.** AIEE TRANSACTIONS, volume 37, 1918, pages 1209–97.

13. SELF-STARTING SYNCHRONOUS MOTORS, **C. J. Fechheimer.** AIEE TRANSACTIONS, volume 31, 1912, pages 529–85.

Practical Calculation of Electrical Transients on Power Systems

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Synopsis: Solution of many power-circuit problems depends upon the magnitude, duration, and frequency of occurrence of transient voltages and currents produced by circuit changes. Circuit-opening or recovery-voltage transients are fundamental in the operation of interrupting devices and may be important from the standpoint of the dielectric strength of circuit elements. Circuit-closing transients may be important from the standpoint of inrush currents and occasionally from the standpoint of overvoltages. Both types of transients may be increased greatly if restriking and repetitive interruptions take place.

Transient voltages and currents resulting from circuit changes can be calculated by the classical method with the aid of differential equations or by the Heaviside method, particularly with the aid of the expansion theorem. Transients on complicated power systems are most conveniently determined by the miniature-system method introduced by Evans and Monteith and described in their 1937 AIEE paper.[4] Analytical methods are useful for simple problems and are essential for those beyond the range of the a-c network calculator or when it is not available. Analytical methods are also necessary for determining the proper form of the equivalent networks for the representation of power systems on the a-c network calculator. This circumstance provided the occasion for the particular investigation for which the present paper is a partial report.

This paper is devoted to the practical calculation of power-system transients and presents concepts, methods, and approximations selected with that objective in view. The paper describes a method of calculation particularly adapted for engineering work.

The scope of the present paper is limited to networks consisting of constant inductance and constant capacitance elements without loss. Consideration of such networks facilitates the presentation of the subject, and gives an adequate view of the phenomena involved. The presentation is from the standpoint of the electrical engineer who is accustomed to making steady-state calculations but who has not given the special study to the transient problem required by the mathematical approach generally used.

THE classical method of determining the transients produced by the application of voltage or current to a network is carried out by setting up a set of linear differential equations. The general solution of the set of equations for the case of a suddenly applied voltage may be written as follows:

$$i(t)=A_o(t)+A_1 \sin \omega_1 t+A_2 \sin \omega_2 t \ldots A_n \sin \omega_n t \quad (1)$$

In this equation, $\omega_1, \omega_2 \ldots \omega_n$ are the modes of oscillation of the network ($2\pi \times$ natural frequencies); $A_1, A_2 \ldots A_n$ are the constants that define the amplitudes of the corresponding modes; and $A_o(t)$ is the term that defines the steady-state current and the nonoscillatory components of the transient current. The integration constants $A_1 \ldots A_n$ can be determined from the boundary conditions in the usual manner. A corresponding equation may be written for the voltages produced by the application of current to a network.

The general solution of the transient problems in networks is readily accomplished in a formal manner, but considerable work remains before the numerical answers are obtained. Determination of the modes of oscillation, although required by all methods (at least for principal terms), involves considerable labor when the equations are of a high degree. Determination of the integration constants, while straightforward from a mathematical point of view, frequently offers considerable difficulty to engineers not accustomed to transient calculations. Because of the limitations of the classical method, the analytical work presented in this paper has been based on the Heaviside expansion theorem. Before taking up the method of analysis described in this paper, it is desirable to discuss in some detail the general properties of no-loss networks.

Properties of Networks

The reactance and susceptance characteristics of networks are fundamental to the method of analysis described in this paper and so will be discussed first. The reactance properties are used when the method is employed in solving circuit-closing problems, whereas the susceptance properties are used in circuit-opening problems. Accordingly, the discussion of networks is arranged in two parts, one for circuit-closing and the other for circuit-opening problems.

All no-loss two-terminal networks consisting of fixed circuit elements are of four types, depending only on the reactances at zero and at infinite frequencies. The principal characteristics of these four types of networks are given in Table I. The network can be typed readily by considering the character of the paths between the driving-point terminals as shown in the table. For example, type I networks have no paths between terminals containing inductance or capacitance alone, that is, all paths contain both elements. Representative networks of the four types are illustrated in the table. Angular velocity-reactance curves corresponding to the representative networks are also given in Table I. These curves are obtained by calculating the steady-state reactance as a function of angular velocity in the usual manner.

The network-reactance curves are discontinuous functions that start from negative infinity and increase to zero or to positive infinity or that start at zero and increase to positive infinity. In other words, all reactance curves have positive slopes[2] throughout their continuous portions. The infinite-reactance points or points of discontinuity are termed "poles," and the zero-reactance points are termed "zeros." These definitions are in accordance with accepted terminology and are used in the subsequent discussion of network characteristics. The "zeros" and "poles" at zero and at infinite frequencies are called "external zeros" and "external poles" and the remaining ones are called "internal zeros" and "internal poles." As can be seen readily from the reactance curves of Table I, the external zeros and poles also define the network type; for example, the type I network has poles at zero and at infinite frequencies. The number of internal zeros that a network has is equal to the number of modes of oscillation; this number is also equal to the maximum number of meshes containing both independent inductive and capacitive elements.

Basic equivalent networks in a form convenient for circuit-closing transients are also included in Table I for each network type. In the basic networks there is one branch containing both L and C for each internal zero; in addition, there are other branches containing only L or only C as required to correspond to the

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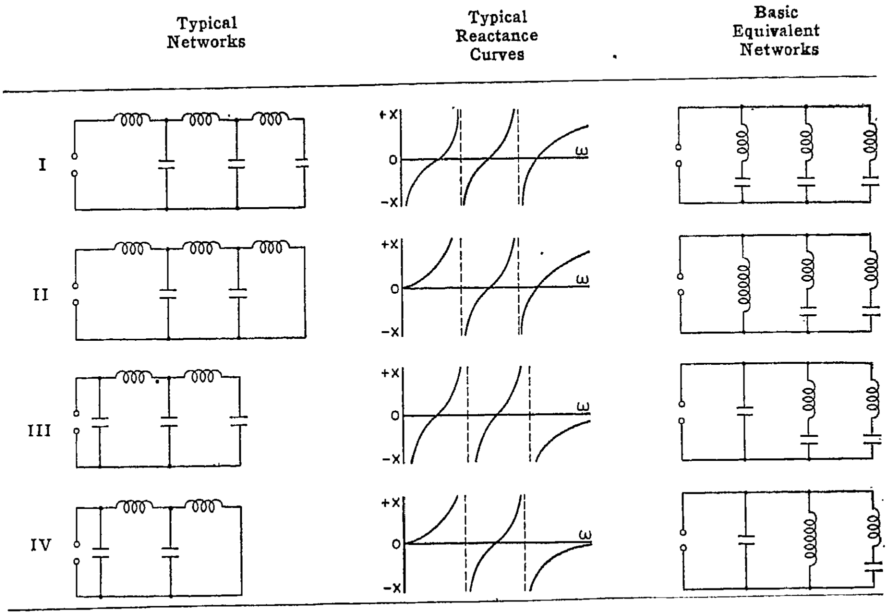
external zeros. A branch containing L alone is required when the network has a zero at zero frequency and a branch containing C alone is required when the network has a zero at infinite frequency. Thus a network of type IV, having zeros at zero and at infinite frequencies, requires one branch with L alone and one branch with C alone.

All no-loss two-terminal networks can also be classified according to the network susceptance at zero and infinite frequencies, as shown in Table II. The representative networks in Table II are identical to the corresponding types in Table I. The angular velocity-susceptance curves are included in the table for each network type. Basic equivalent networks of the form convenient for circuit-opening transients are also included in Table II.

Table I. Classification of Networks for Circuit-Closing Transients

Type	X at $\omega=\infty$	X at $\omega=0$	Paths: C Alone	Paths: L Alone
I	∞	∞	None	None
II	∞	0	None	At least one
III	0	∞	At least one	None
IV	0	0	At least one	At least one



Method of Zeros and Slopes

As the method of analysis to be described depends upon the zeros and slopes of the reactance (or susceptance) curves for the networks, it is referred to as the method of "zeros and slopes." The method is described under two headings:

(*a*). Unit voltage applied.
(*b*). Unit current applied.

The method will be extended for the application of a-c voltages and currents in a later section.

(a). UNIT VOLTAGE APPLIED

The response of a network to a suddenly applied unit voltage is given by the Heaviside expansion theorem as follows:

$$i(t)=i_o(t)+\sum_{k=1}^{k=2n}\frac{e^{p_kt}}{p_kZ'(p_k)} \qquad (2)$$

In this form of the expansion theorem, the function $i_o(t)$ defines the steady-state network solution and that part of the transient solution determined by the external zeros. The term $Z'(p)$ is the first derivative of $Z(p)$ with respect to p, where $Z(p)$ is the impedance function of the network as viewed from the driving point. The summation indicates that there are as many terms in the transient solution (neglecting the $i_o(t)$ term) as there are roots of the equation $Z(p)=0$. The quantity p_k is a general expression for the roots of $Z(p)=0$.

It is shown in the appendix that when restricted to no-loss networks, equation 2 can be written in the following approximate form:

$$i(t)=i_o(t)+A_1\sin\omega_1t+A_2\sin\omega_2t+\ldots+A_n\sin\omega_nt \qquad (3)$$

where

$$A_1=\frac{2}{\omega_1\dfrac{\Delta X}{\Delta\omega_1}};\quad A_2=\frac{2}{\omega_2\dfrac{\Delta X}{\Delta\omega_2}};\quad\ldots A_n=\frac{2}{\omega_n\dfrac{\Delta X}{\Delta\omega_n}}$$

The $i_o(t)$ term defines the steady-state current and the components of the transient current corresponding to the external zeros. This term is given in Table III for each type of network. The term t/L_o defines a current starting at zero and increasing directly with time to an infinite value at $t=\infty$. The term $\left[\dfrac{t}{C_o}\right]_{t=0}$ defines a current having an infinite magnitude at $t=0$ and equal to zero thereafter, such as instantly to bring the charge on the capacitance to the applied voltage. The quantity L_o is defined as the equivalent inductance between driving-point terminals with all capacitances open-circuited, and C_o is the equivalent capacitance with all inductances open-circuited.

In equation 3, ω_1, ω_2, ... ω_n are the values of angular velocity corresponding to zero reactance between the driving-point terminals, and $\dfrac{\Delta X}{\Delta\omega_1}$, $\dfrac{\Delta X}{\Delta\omega_2}$, ... $\dfrac{\Delta X}{\Delta\omega_n}$ are the slopes of the angular velocity-reactance curve at the corresponding values of angular velocity. This relation shows that the response of a no-loss network can be determined from the steady-state reactance characteristics of the network. An approximate solution can, therefore, be obtained by calculating the angular velocity-reactance curve and then determining the slopes of the curve at or near the zeros. This procedure is illustrated in Figure 1 for a relatively simple circuit. It should be noted that it is not necessary to obtain the complete curve but only those parts in the vicinity of the zeros. The number of points that must be calculated will depend upon the accuracy desired and on the ability of the operator to estimate the zeros. The accuracy of the solution will depend entirely on the accuracy with which the zeros and slopes are evaluated.

(b). UNIT CURRENT APPLIED

The problem of calculating the transient voltage following the opening of a circuit can be analyzed in terms of the original current and a suddenly applied equal and opposite current.* The superposition of these two currents reduces the total current to zero, which is equivalent to opening the circuit. Thus, the first step is to calculate the network response to unit current.

The response of a network to a suddenly applied unit current can also be written in a form similar to equation 3.

$$e(t)=e_o(t)+A_a\sin\omega_at+A_b\sin\omega_bt+\ldots+A_m\sin\omega_mt \qquad (4)$$

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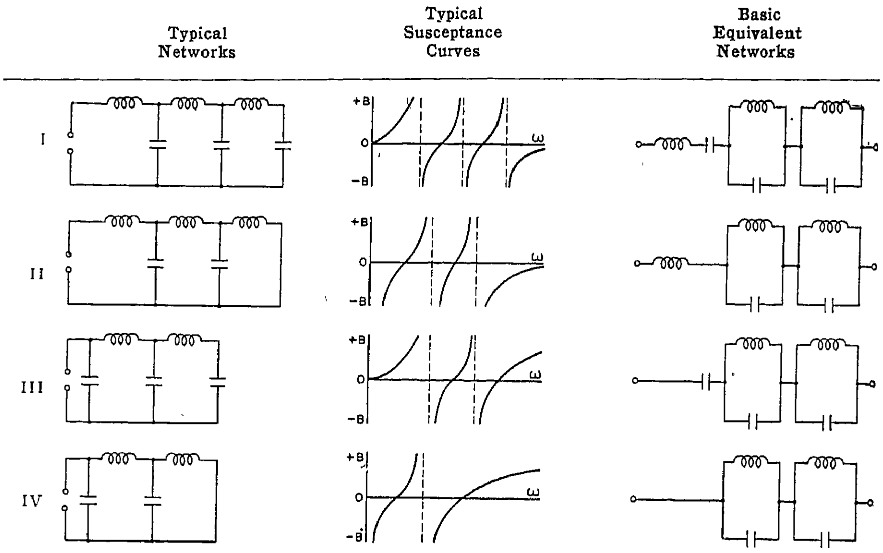
* Page 306 of reference 1.

Table II. Classification of Networks f r Circuit-Opening Transients

Type	B at $\omega=\infty$	B at $\omega=0$	Paths: C Alone	Paths: L Alone
I	0	0	None	None
II	0	∞	None	At least one
III	∞	0	At least one	None
IV	∞	∞	At least one	At least one



where

$$A_a=\frac{2}{\omega_a\frac{\Delta B}{\Delta\omega_a}};\ A_b=\frac{2}{\omega_b\frac{\Delta B}{\Delta\omega_b}};\ \ldots A_m=\frac{2}{\omega_m\frac{\Delta B}{\Delta\omega_m}}$$

The $e_o(t)$ term defines the steady-state voltage and the components of the transient voltage corresponding to the external zeros. This term is given in Table III for each type of network. The term t/C_s defines a voltage starting at zero and increasing directly with time to an infinite value at $t=\infty$. The term $\left[\frac{L_s}{t}\right]_{t=0}$ defines a voltage having an infinite magnitude at $t=0$ and equal to zero thereafter. The quantity C_s is the equivalent capacitance between driving-point terminals with all inductances short-circuited, and L_s is the equivalent inductance with all capacitances short-circuited.

Table III

Network Type	$i_0(t)$	$e_0(t)$
I	0	$\left[\frac{L_s}{t}\right]_{t=0}+\frac{t}{C_s}$
II	$\frac{t}{L_o}$	$\left[\frac{L_s}{t}\right]_{t=0}$
III	$\left[\frac{C_o}{t}\right]_{t=0}$	$\frac{t}{C_s}$
IV	$\frac{t}{L_o}+\left[\frac{C_o}{t}\right]_{t=0}$	0

L_s=Equivalent L determined with all C's short-circuited
L_o=Equivalent L determined with all C's open-circuited
C_o=Equivalent C determined with all L's open-circuited
C_s=Equivalent C determined with all L's short-circuited

In equation (4), ω_a, ω_b, … ω_m are the values of angular velocity resulting in zero susceptance between driving-point terminals, and $\frac{\Delta B}{\Delta\omega_a}$, $\frac{\Delta B}{\Delta\omega_b}$, … $\frac{\Delta B}{\Delta\omega_m}$ are the slopes of the angular velocity-susceptance curve at the corresponding points. The response of a no-loss network to unit current can, therefore, be obtained in a manner similar to that illustrated for the response to unit voltage. However, in this case the steady-state susceptance characteristics of the network are used instead of the reactance characteristics.

Equivalent Networks

Every network can be reduced to an equivalent network[2,3] of either the shunt or series forms, illustrated in Figure 2. The basic equivalent network of the shunt form is advantageous for circuit-closing transients because of the lack of coupling between branches for this type of circuit change. Similarly, the basic equivalent network of the series form is advantageous for circuit-opening transients. These equivalent networks are helpful in visualizing the properties of networks and in interpreting the equations of transient response. Furthermore, the replacement of a given complicated network by the simplest equivalent network of a given form is advantageous under many circumstances for calculation by analytical or a-c network calculator methods.[4]

(a). Circuit-Closing Transients

The basic equivalent network in the shunt form of Table I can be derived from the network response to unit voltage as given by equation 3. For convenience this equation has been rewritten with the $i_o(t)$ term expanded as given in Table III:

$$i(t)=\frac{t}{L_o}+\left[\frac{C_o}{t}\right]_{t=0}+A_1\sin\omega_1 t+ A_2\sin\omega_2 t\ldots A_n\sin\omega_n t \quad (5a)$$

$$=\frac{t}{L_o}+\left[\frac{C_o}{t}\right]_{t=0}+\frac{2}{\omega_1\frac{\Delta X}{\Delta\omega_1}}\sin\omega_1 t+ \frac{2}{\omega_2\frac{\Delta X}{\Delta\omega_2}}\sin\omega_2 t\ldots\frac{2}{\omega_n\frac{\Delta X}{\Delta\omega_n}}\sin\omega_n t \quad (5b)$$

The basic equivalent network of the shunt form is illustrated in Figure 2*a*. Each branch of this equivalent network corresponds to one term of the network solution given by equation 5. The L_o and C_o branches correspond, respectively, to an equivalent inductance or an equivalent capacitance in parallel with the driving-point terminals, as defined in Table III. As the response of a simple series LC circuit to unit voltage is $\sqrt{\frac{C}{L}}\sin\omega t$, where $\omega=\frac{1}{\sqrt{LC}}$, equation 5 can be written in the following form:

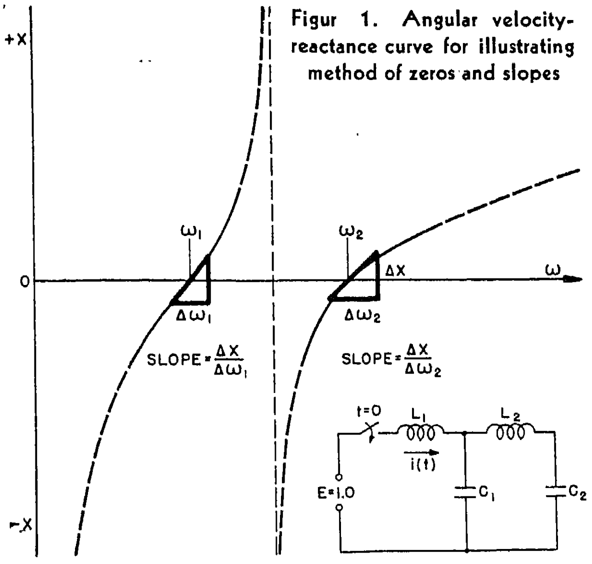
$$i(t)=\frac{t}{L_o}+\left[\frac{C_o}{t}\right]_{t=0}+\sqrt{\frac{C_1}{L_1}}\sin\omega_1 t+ \sqrt{\frac{C_2}{L_2}}\sin\omega_2 t\ldots\sqrt{\frac{C_n}{L_n}}\sin\omega_n t \quad (6)$$

where

$$\omega_1=\frac{1}{\sqrt{L_1C_1}};\ \omega_2=\frac{1}{\sqrt{L_2C_2}};\ \ldots\ \omega_n=\frac{1}{\sqrt{L_nC_n}}$$

By equating the coefficients of equations 5*a* and 6, the remaining circuit elements of the equivalent network can be determined as follows:

$$\left.\begin{array}{l}A_1=\sqrt{\frac{C_1}{L_1}};\ A_2=\sqrt{\frac{C_2}{L_2}};\ \ldots\ A_n=\sqrt{\frac{C_n}{L_n}}\\ \omega_1=\frac{1}{\sqrt{L_1C_1}};\ \omega_2=\frac{1}{\sqrt{L_2C_2}};\ \ldots\omega_n=\frac{1}{\sqrt{L_nC_n}}\\ C_1=\frac{A_1}{\omega_1};\ C_2=\frac{A_2}{\omega_2};\ \ldots\ C_n=\frac{A_n}{\omega_n}\\ L_1=\frac{1}{\omega_1A_1};\ L_2=\frac{1}{\omega_2A_2};\ \ldots\ L_n=\frac{1}{\omega_nA_n}\end{array}\right\} \quad (7)$$



Figur 1. Angular velocity-reactance curve for illustrating method of zeros and slopes

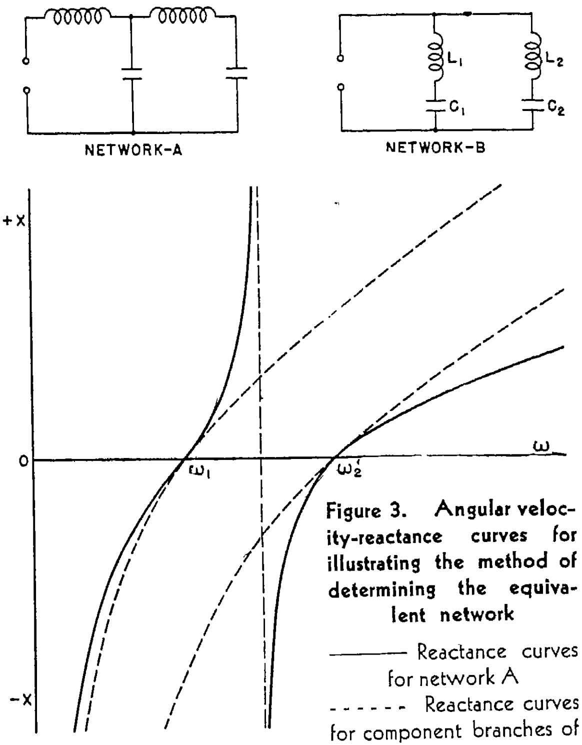
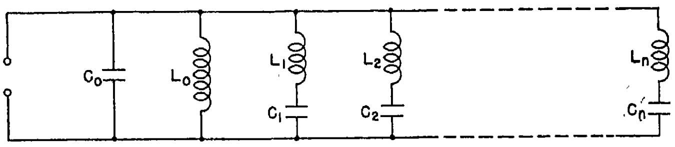


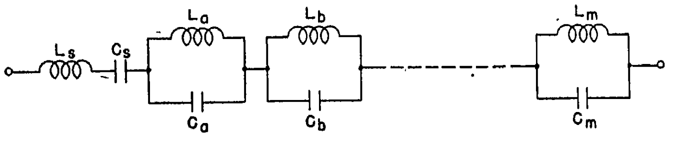
Figure 3. Angular velocity-reactance curves for illustrating the method of determining the equivalent network

——— Reactance curves for network A

- - - - - - Reactance curves for component branches of equivalent network B



(a) SHUNT FORM



(b) SERIES FORM

Figure 2 (left). Basic equivalent networks in the shunt and series forms

By equating the coefficients of equations 5*b* and 6, the circuit elements of the equivalent network can be stated directly in terms of the zeros and slopes:

$$\left.\begin{array}{l}
\sqrt{\dfrac{C_1}{L_1}}=\dfrac{2}{\omega_1\dfrac{\Delta X}{\Delta\omega_1}};\quad \sqrt{\dfrac{C_2}{L_2}}=\dfrac{2}{\omega_2\dfrac{\Delta X}{\Delta\omega_2}};\\
\qquad\qquad \ldots\sqrt{\dfrac{C_n}{L_n}}=\dfrac{2}{\omega_n\dfrac{\Delta X}{\Delta\omega_n}}\\
\omega_1=\dfrac{1}{\sqrt{L_1C_1}};\quad \omega_2=\dfrac{1}{\sqrt{L_2C_2}};\\
\qquad\qquad \ldots\omega_n=\dfrac{1}{\sqrt{L_nC_n}}\\
L_1=\dfrac{1}{2}\dfrac{\Delta X}{\Delta\omega_1};\quad L_2=\dfrac{1}{2}\dfrac{\Delta X}{\Delta\omega_2};\quad \ldots L_n=\dfrac{1}{2}\dfrac{\Delta X}{\Delta\omega_n}\\
C_1=\dfrac{1}{\omega_1{}^2L_1};\quad C_2=\dfrac{1}{\omega_2{}^2L_2};\quad \ldots C_n=\dfrac{1}{\omega_n{}^2L_n}
\end{array}\right\} \tag{8}$$

These relations show that the circuit elements of the equivalent network can be determined directly from the angular velocity-reactance curves. This is graphically illustrated in Figure 3 for a simple two-mesh network. The reactance curves are shown for the original network and for each branch of the equivalent network. As the two networks are equivalent the reactance of network *A* is equal to the reactance of network *B*, which, of course, is equal to the parallel of the reactances of the individual branches. Each zero for the reactance of the total network occurs at the zero for a component branch and the corresponding slopes at these zeros are identical. This results from the fact that at the zero for one branch the reactance of the other branch becomes negligibly high in comparison with the branch under consideration. Thus, for each internal zero of the angular velocity-reactance curve there is an equivalent shunt branch containing *L* and *C*, the values of which are defined by the corresponding zero and slope. This same reasoning can be extended to any number of branches.

(b). Circuit-Opening Transients

The basic equivalent network in the series form of Table II can readily be derived from equation 4 for the response to unit current applied. For convenience this equation has been rewritten with the $e_o(t)$ term expanded as given in Table III:

$$e(t)=\frac{t}{C_s}+\left[\frac{L_s}{t}\right]_{t=}+A_a\sin\omega_at+ \\ A_b\sin\omega_bt\ldots+A_m\sin\omega_mt \tag{9a}$$

$$=\frac{t}{C_s}+\left[\frac{L_s}{t}\right]_{t=0}+\frac{2}{\omega_a\dfrac{\Delta B}{\Delta\omega_a}}\sin\omega_at+ \\ \frac{2}{\omega_b\dfrac{\Delta B}{\Delta\omega_b}}\sin\omega_bt\ldots\frac{2}{\omega_m\dfrac{\Delta B}{\Delta\omega_m}}\sin\omega_mt \tag{9b}$$

Table IV. Unit Voltage Applied

(1) ω	(2) ωL_2	(3) $1/\omega C_2$	(4) (2)+(3)	(5) 1/(4)	(6) ωC_1	(7) (5)+(6)	(8) 1/(7)	(9) ωL_1	X (8)+(9)
				$x10^{-4}$	$x10^{-4}$	$x10^{-4}$			
2,701	2,701	−3,702.3	−1,001.3	9.987	27.01	36.997	−270.29	270.1	−0.19
2,702	2,702	−3,700.9	−998.9	10.011	27.02	37.031	−270.04	270.2	+0.16
3,701	3,701	−2,701.9	−999.1	10.009	37.01	27.001	−370.35	370.1	−0.25
3,702	3,702	−2,701.2	−1,000.8	9.992	37.02	27.028	−369.98	370.2	+0.22

$\omega_1=2{,}701$

$\Delta\omega_1=1.0$

$\Delta X=0.19+0.16=0.35$

$\dfrac{\Delta X}{\Delta\omega_1}=0.35$

$\dfrac{2}{\omega_1\dfrac{\Delta X}{\Delta\omega_1}}=0.00212$

$\omega_2=3{,}701$

$\Delta\omega_2=1.0$

$\Delta X=0.47$

$\dfrac{\Delta X}{\Delta\omega_2}=0.47$

$\dfrac{2}{\omega_2\dfrac{\Delta X}{\Delta\omega_2}}=0.00115$

$i(t)=0.00212\sin 2{,}701t+0.00115\sin 3{,}701t$

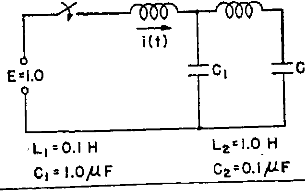
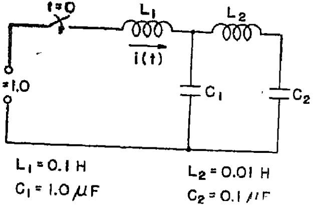


Table V. Unit Voltage Applied

ω	X	$\frac{\Delta X}{\Delta\omega}$
3,013	−0.17	
3,014	+0.03	0.20
33,100	+4,073	
33,200	+1,969	
33,179	−191	
33,180	+61	252

$i(t)=0.00$[illegible] sin 3,014t + 0.00000024 sin 33,179t



The basic equivalent network of the series form is illustrated in Figure 2*b*. Each branch of this equivalent network corresponds to one term of the network solution given by equation 9. The L_s and C_s branches correspond to an equivalent inductance or an equivalent capacitance in series with the driving point, as defined in Table III. As the response of a simple parallel LC circuit to unit current is equal to $\sqrt{\frac{L}{C}}\sin\omega t$, where $\omega=\frac{1}{\sqrt{LC}}$, equation 9 can be written in the following form:

$$e(t)=\frac{t}{C_s}+\left[\frac{L_s}{t}\right]_{t=0}+\sqrt{\frac{L_a}{C_a}}\sin\omega_a t+\sqrt{\frac{L_b}{C_b}}\sin\omega_b t\ldots\sqrt{\frac{L_m}{C_m}}\sin\omega_m t \quad (10)$$

where

$$\omega_a=\frac{1}{\sqrt{L_aC}};\ \omega_b=\frac{1}{\sqrt{L_bC_b}};\ldots\omega_m=\frac{1}{\sqrt{L_mC_m}}$$

By equating the coefficients of equations 9*a* and 10, the remaining circuit elements of the equivalent network can be determined as follows:

$$\left.\begin{aligned}&A_a=\sqrt{\frac{L_a}{C_a}};\quad A_b=\sqrt{\frac{L_b}{C_b}};\quad\ldots A_m=\sqrt{\frac{L_m}{C_m}}\\&\omega_a=\frac{1}{\sqrt{L_aC_a}};\quad\omega_b=\frac{1}{\sqrt{L_bC_b}};\\&\qquad\ldots\omega_m=\frac{1}{\sqrt{L_mC_m}}\\&C_a=\frac{1}{\omega_aA_a};\quad C_b=\frac{1}{\omega_bA_b};\quad\ldots C_m=\frac{1}{\omega_mA_m}\\&L_a=\frac{A_a}{\omega_a};\quad L_b=\frac{A_b}{\omega_b};\quad\ldots L_m=\frac{A_m}{\omega_m}\end{aligned}\right\}\quad(11)$$

By equating coefficients of equations 9*b* and 10, the circuit elements of equivalent networks can be stated directly in terms of the zeros and slopes:

$$\left.\begin{aligned}&\sqrt{\frac{L_a}{C_a}}=\frac{2}{\omega_a\frac{\Delta B}{\Delta\omega_a}};\quad\sqrt{\frac{L_b}{C_b}}=\frac{2}{\omega_b\frac{\Delta B}{\Delta\omega_b}};\\&\qquad\ldots\sqrt{\frac{L_m}{C_m}}=\frac{2}{\omega_m\frac{\Delta B}{\Delta\omega_m}}\\&\omega_a=\frac{1}{\sqrt{L_aC_a}};\quad\omega_b=\frac{1}{\sqrt{L_bC_b}};\\&\qquad\ldots\omega_m=\frac{1}{\sqrt{L_mC_m}}\\&C_a=\frac{1}{2}\frac{\Delta B}{\Delta\omega_a};\quad C_b=\frac{1}{2}\frac{\Delta B}{\Delta\omega_b};\ldots C_m=\frac{1}{2}\frac{\Delta B}{\Delta\omega_m}\\&L_a=\frac{1}{\omega_a^2C_a};\quad L_b=\frac{1}{\omega_b^2C_b};\quad\ldots L_m=\frac{1}{\omega_m^2C_m}\end{aligned}\right\}\quad(12)$$

In the preceding discussion the basic equivalent networks have been derived from network-response equations based on the Heaviside expansion theorem. By postulating that every network can be reduced to one of the basic forms of equivalent circuits, it is possible to derive the network response equations 3 and 4 without the use of the expansion theorem. Thus, the equivalent circuits provide not only a means of visualizing the network response but also the basis for an alternative derivation of the response equations.

Table VI. Alternating Current Applied

(1) ω	(2) ωL_2	(3) $1/\omega C_2$	(4) (2)+(3)	(5) 1/(4)	(6) ωC_1	(7) $1/\omega L_1$	B (5)+(6)+(7)
				$x10^{-4}$	$x10^{-4}$	$x10^{-4}$	$x10^{-4}$
2,701	2,701	−3,702.30	−1,001.30	9.987	27.01	−37.023	−0 026
2,702	2,702	−3,700.96	−998.96	10.011	27.02	−37.009	+0.021
3,701	3,701	−2,701.97	999.03	−10.009	37.01	−27.019	−0.018
3,702	3,702	−2,701.24	1,000.80	−9.992	37 02	−27.012	+0.016
377	377	−26,500	−26,100	0.383	3.77	−265	−261

$\omega_a=2{,}701$ $\qquad\omega_b=3{,}701$

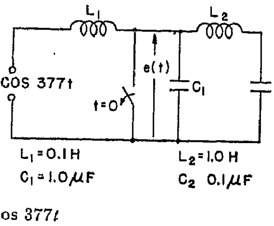
$\frac{\Delta B}{\Delta\omega_a}=0.047\times10^{-4}$ $\qquad\frac{\Delta B}{\Delta\omega_b}=0.034\times10^{-4}$

$A_a=157$ $\qquad A_b=159$

Fault current $=\frac{100}{j37.7}\cos 377t$

$=2.65\cos\left(377t-\frac{\pi}{2}\right)$

$I(t)=2.65\cos\left(377t-\frac{\pi}{2}\right)$



Steady-state component of $e(t)=\frac{2.65}{-j0.0261}\cos\left(377t-\frac{\pi}{2}\right)=102\cos 377t$

$e(t)=102\cos 377t-59\cos 2{,}701t-44\cos 3{,}701t$

Alternating Voltages and Currents Applied

The response of a network to alternating voltages or currents can be obtained from the response to unit voltage or current by superposition, or from the basic equivalent networks. The equations relating the alternating and unit-function responses have been derived and are included here for reference.

(a). ALTERNATING VOLTAGE APPLIED

Let

$$i(t)=\frac{t}{L_o}+\left[\frac{C_o}{t}\right]_{t=0}+\sum_{k=1}^{k=n}A_k\sin\omega_kt$$

be the network response to unit voltage. The response of the network to the sudden application of the voltage $E(t)=E\cos(\omega_ot+\phi)$ is then defined by the following equation:

$$\left.\begin{aligned}i(t)=&\left[\frac{E}{\omega_oL_o}\sin(\omega_ot+\phi)-\frac{E}{\omega_oL_o}\sin\phi\right]+\\&\left[-E\omega_oC_o\sin(\omega_ot+\phi)+\left(\frac{EC_o\cos\phi}{t}\right)_{t=0}\right]+\\&\left[-E\sum_{k=1}^{k=n}\frac{\omega_o\omega_kA_k}{(\omega_k^2-\omega_o^2)}\sin(\omega_ot+\phi)+\right.\\&\left.E\sum_{k=1}^{k=n}\frac{\omega_k^2A_k}{(\omega_k^2-\omega_o^2)}\left(\cos\phi\sin\omega_kt+\frac{\omega_o}{\omega_k}\sin\phi\cos\omega_kt\right)\right]\end{aligned}\right\}\quad(13)$$

Each of the bracketed terms in the preceding equation is the a-c solution corresponding to one term in the unit voltage response equation or for one branch of the equivalent network. The first term in each bracket is a component of the steady-state response of the network, whereas the second term is in each case a transient response. In many cases less work is involved in determining the steady-state response directly from the network reactance at $\omega=\omega_o$. If this reactance is equal to $X(j\omega_o)$, then the steady-state response is equal to

$$\frac{E}{X(j\omega_o)}\cos(\omega_ot+\phi)$$

(b). Alternating Current Applied

Let

$$e(t)=\frac{t}{C_s}+\left[\frac{L_s}{t}\right]_{t=0}+\sum_{k=1}^{k=n}A_k\sin\omega_k t$$

be the response for unit current applied. The network response to the current $I(t)=I\cos(\omega_o t+\phi)$ is then defined by the following relations:

$$\left.\begin{aligned}e(t)=&\left[\frac{I}{\omega_o C_s}\sin(\omega_o t+\phi)-\frac{I}{\omega_o C_s}\sin\phi\right]+\\&\left[-I\omega_o L_s\sin(\omega_o t+\phi)+\left(\frac{IL_s\cos\phi}{t}\right)_{t=0}\right]+\\&\left[-I\sum_{k=1}^{k=n}\frac{\omega_o\omega_k A_k}{(\omega_k^2-\omega_o^2)}\sin(\omega_o t+\phi)+\right.\\&\quad I\sum_{k=1}^{k=n}\frac{\omega_k^2 A_k}{(\omega_k^2-\omega_o^2)}\left(\cos\phi\sin\omega_k t+\right.\\&\qquad\left.\left.\frac{\omega_o}{\omega_k}\sin\phi\cos\omega_k t\right)\right]\end{aligned}\right\}\tag{14}$$

Each of the bracketed terms in the preceding equation is the a-c solution corresponding to one term of the response equation for unit current applied, or for one branch of the equivalent network. The first term in each bracket is the steady-state response, and the second term is the transient response. The steady-state response can also be obtained from the network admittance for $\omega=\omega_o$. If this admittance is represented by $B(j\omega_o)$, the steady-state response is equal to

$$\frac{I}{B(j\omega_o)}\cos(\omega_o t+\phi)$$

One special case of the foregoing solution is of general interest, the case where the applied current is equal to $I\sin\omega_o t=I\cos(\omega t-\pi/2)$:

$$\left.\begin{aligned}e(t)=&\left[-\frac{E}{\omega_o C_s}\cos\omega_o t+\frac{I}{\omega_o C_s}\right]+\\&\left[I\omega_o L_s\cos\omega_o t\right]+\\&\left[I\sum_{k=1}^{k=n}\frac{\omega_o\omega_k A_k}{(\omega_k^2-\omega_o^2)}\cos\omega_o t-\right.\\&\quad\left.I\sum_{k=1}^{k=n}\frac{\omega_o\omega_k A_k}{(\omega_k^2-\omega_o^2)}\cos\omega_k t\right]\end{aligned}\right\}\tag{15}$$

Illustrative Examples

The method of analysis just described is illustrated in Table IV for a relatively simple circuit. The problem in this case is to find an analytical expression for the current in the inductance L_1 when unit voltage is suddenly applied to the driving-point terminals. The zeros are first located approximately by rough slide-rule calculations, preferably using a plot of the reactance curve as a guide. The reactance of the circuit, as viewed from the driving point, is calculated by combining the reactance of the individual branches as illustrated in the table. It should be noted that it is not necessary to obtain the complete reactance curve but only those parts of the curve in the vicinity of zero reactance. Only two points near each zero are included in the illustration, these points having been arrived at after other preliminary points were obtained. The reactance curves need not be plotted except as a guide; all necessary information is included in the tabulated data. In this example $\Delta\omega$ was taken as one radian per second in order to obtain an accurate evaluation of $\Delta X/\Delta\omega$. The angular velocity increment to use in any particular case depends upon the network characteristics and upon the accuracy desired. It should be stressed that the reactance calculations should be carried out to a high degree of accuracy if a high degree of accuracy is desired in the final network response. However, if practical engineering accuracy is sufficient then less accuracy is required in calculating the reactances.

After the zeros and slopes are obtained the response is calculated by substituting these values in equation 3. As the network considered in this example is of type I, $i_o(t)$ is equal to zero and so the complete response is as given at the bottom of Table IV.

The accuracy of the method can readily be checked by noting the initial conditions. At $t=0$, the voltage across C_1 is zero, so the applied unit voltage must be consumed across L_1, that is, $L_1\dfrac{di(t)}{dt}$ should be equal to unity at $t=0$. For Table IV, the check gives

$$L_1\frac{di(t)}{dt}=0.1(2{,}701\times 0.00212+3{,}701\times 0.00115)=0.998 \text{ at } t=0$$

which is accurate to within 0.2 of one per cent.

In the general case the calculated responses for networks of types I and II are checked by noting that $L_s\dfrac{di(t)}{dt}=1.0$ at $t=0$, assuming unit voltage applied, where L_s is the equivalent inductance between the driving-point terminals with all capacitances short-circuited.

In special cases some difficulty may be experienced in locating the zeros. For example, the reactance of the network in Table V was calculated at $\omega=33{,}100$ and 33,200 and the resulting reactances were both positive and decreasing in magnitude. These results incorrectly may be assumed to indicate that a zero would be present at some higher angular velocity, whereas the zero is actually between these values of angular velocity. When such cases are encountered much time is usually required to locate the zero. However, when the zeros are hard to locate, the slope of the reactance curve is large and as a result the transient current corresponding to the zero may be an unimportant component of the network response. In Table V the second component of current is only 0.00723 per cent of the first component and so is insignificant. This situation could have been predicted by checking the first component of current with the initial conditions. Neglecting the second term,

$$L_1\frac{di(t)}{dt}=0.1(0.00332)(3{,}014)=1.0 \text{ at } t=0$$

which satisfies the boundary conditions. It is very helpful to make these checks for each component as soon as it is obtained, in order to obtain not only a check on the integrity of the calculation but also a continuous indication of the relative importance of the remaining terms of the solution.

The problem of calculating the transient voltage following the sudden opening of a switch can be treated in terms of a suddenly applied current. Considering the network in Table VI, the current flowing in the closed switch is equal to +2.65 sin 377t. The current in the switch can be reduced to zero, which is equivalent to opening the switch, by inserting a current +2.65 sin 377t in a direction opposite to the initial current. In making the calculations with the method of zeros and slopes, the response to unit current is first obtained, as illustrated in Table VI, by determining the zeros and slopes of the susceptance curves. The response to the current +2.65 sin 377t is then found by using the relations given in part *b* of the preceding section. The steady-state component of the recovery voltage was found by first calculating the network susceptance at $\omega=377$.

Extensions of the Method

In the paper the calculating procedure has been illustrated only for the zeros and slopes derived from the constants of the networks. However, it is possible and sometimes convenient to form the analytical expression for the impedance (or admittance) function and its derivative and to use the first or both of these to determine the zeros and slopes. The method of analysis and calculating procedure have been discussed only for problems involving driving-point functions. However, they are also applicable to problems involving transfer functions, for example, the determination of current in a particular branch of a network resulting from the application of potential to the driving-point terminals. The procedure is the same except that for the latter case the zeros and slopes are obtained from the transfer function instead of the driving-point function. Angular velocity-reactance (or susceptance) curves, derived from measurements on a miniature-system setup or even from an actual system instead of from calculated data as described in the paper, can be used for the determination of zeros and slopes.

Appendix

The response of a network to a suddenly applied unit voltage is given by the Heaviside expansion theorem as follows:

$$i(t)=i_o(t)+\sum_{k=1}^{k=2n}\frac{e^{p_kt}}{p_kZ'(p_k)} \qquad (1)$$

In this form of the expansion theorem the function $i_o(t)$ defines the steady-state network solution and that part of the transient solution determined by the external zeros. Referring to the second term, $Z(p)$ is the impedance function of the network as viewed from the driving point and $Z'(p)$ is the first derivative of the impedance function with respect to p. The summation indicates that there are as many terms in the transient solution (neglecting the $i_o(t)$ term) as there are roots of the equation $Z(p)=0$. The quantity p_k is a general expression for the roots of $Z(p)=0$.

In a no-loss network the roots of $Z(p)=0$ are pure imaginaries appearing in conjugate pairs and so can be represented by $p=\pm j\omega$. It should also be noted that in no-loss network $Z'(j\omega_k)=Z'(-j\omega_k)$. Substituting these relations into the expansion theorem reduces equation 1 to

$$i(t)=i_o(t)+\sum_{k=1}^{k=n}\frac{e^{j\omega_kt}}{j\omega_kZ'(j\omega_k)}-\sum_{k=1}^{k=n}\frac{e^{-j\omega_kt}}{j\omega_kZ'(j\omega_k)} \qquad (2)$$

This form of the theorem can then be converted to the trigonometric form, remembering that

$$\frac{e^{j\omega t}-e^{-j\omega t}}{2j}=\sin\omega t,$$

$$i(t)=i_o(t)+\sum_{k=1}^{k=n}\frac{2}{\omega_kZ'(j\omega_k)}\sin\omega_kt \qquad (3)$$

In a no-loss network $Z(j\omega)$ is recognized as the alternating-current reactance of the network as measured at the driving point, and $\omega_k=\omega_1, \omega_2 \ldots n$ are the angular velocities that give zero reactance. The term $Z'(j\omega)$ is, therefore, the slope of the reactance curve plotted as a function of $j\omega$, and $Z'(j\omega_k)$ is the slope at $\omega=\omega_k$. It becomes apparent, therefore, that the constants of integration, or the coefficients of the series of sine functions, depend only on the angular velocities resulting in zero reactance, and on the slope of the reactance curve at these corresponding velocities.

In making numerical computations, equation 3 is more useful in its approximate form,

$$i(t)=i_o(t)+\sum_{k=1}^{k=n}\frac{2}{\omega_k\dfrac{\Delta X}{\Delta\omega_k}}\sin\omega_kt \qquad (4)$$

or with alternative notation,

$$i(t)=i_o(t)+A_1\sin\omega_1t+A_2\sin\omega_2t+\ldots A_n\sin\omega_nt \qquad (5)$$

where

$$A_1=\frac{2}{\omega_1\Delta X/\Delta\omega_1};\quad A_2=\frac{2}{\omega_2\Delta X/\Delta\omega_2};\ldots$$

$$A_n=\frac{2}{\omega_n\Delta X/\Delta\omega_n}$$

References

1. OPERATIONAL CIRCUIT ANALYSIS (book), **Vannevar Bush.** John Wiley and Sons, Inc., New York, N. Y., 1929.

2. A REACTANCE THEOREM, **R. M. Foster.** *Bell System Technical Journal*, April 1924, pages 259–67.

3. COMMUNICATION NETWORKS, volume II (book), **E. A. Guillemin.** John Wiley and Sons, Inc., New York, N. Y., 1935.

4. SYSTEM RECOVERY VOLTAGE DETERMINATION BY ANALYTICAL AND A-C CALCULATING BOARD METHODS, **R. D. Evans, A. C. Monteith.** AIEE TRANSACTIONS, volume 56, 1937, June section, pages 695–705.

The Cause and Control of Some Types of Switching Surges

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AMONG the several causes other than lightning which can result in power-system overvoltages, switching surges rank high in importance. In the past the magnitudes of switching surges have been considered on a statistical basis, but the need for frequent switching operations naturally led to their study and to analyses of their causes and magnitudes.

Switching surges are transient overvoltages caused by closing or opening a circuit and can vary in magnitude and duration over wide ranges. As contrasted with lightning surges which are generally impulsive and unidirectional in character, switching surges are oscillatory in nature, of frequency anywhere from very high to fundamental or subharmonic frequency, and of duration from a small portion of a cycle to a relatively long time which may be governed by the time between the opening or closing of successive phases on a three-phase system. Where steepness of voltage wave front is a consideration, it should be recognized that surges caused by switching on certain circuits may have wave fronts steeper than the ordinary lightning surge.

Switching in circuits supplying ungrounded transformer banks can result in high voltages during the switching period if the interval between closing or opening the first and last phase in a three-phase system is long enough for the intermediate steady-state voltage conditions to be attained. This condition may obtain during single-phase switching on a three-phase system, or when one or two conductors are open by virtue of conductor breaks or blown fuses. The voltages obtained under these conditions have been studied and reported[1] for a wide range of constants. The principal constants which apply are the transformer magnetizing reactance, the system capacitance between the switch and the transformer, and the degree of transformer saturation. This paper in general, will, consider over-voltages other than these, which occur even with reasonably simultaneous action of the switches in the three poles. Primary consideration will be given to transient voltages caused by normal rather than fault switching operations.

Conclusions Drawn

Some of the conclusions which may be drawn from the analyses and discussion are as follows:

1. When a switch on an unfaulted power system is being closed, transient voltages as

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high as nearly twice normal line-to-neutral voltage may be obtained. This assumes that closing of the three poles of the switch is reasonably simultaneous, as would be true of the ordinary circuit breaker.

2. Transformer windings can be very effective in discharging the stored energy of a capacitive circuit being opened and thus reduce the possibility and magnitude of restriking overvoltages.

If the breaker is connected to the line, cable, or capacitor bank through a delta-wye-grounded transformer bank as shown in Figure 3B, the overvoltage under restriking conditions will be limited to less than twice normal line-to-ground voltage if the transformer has a kilovolt-ampere rating equal to or greater than the charging kilovolt-amperes of the capacitive circuit. Simultaneous clearing of the three phases is not required for this statement to hold true.

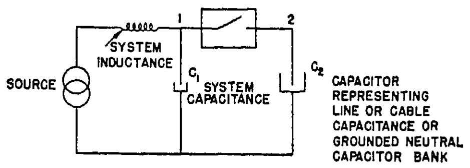
Where switching is done at line voltage, transformer windings switched with the line, as shown in Figure 3A, are also effective if the ratio of the capacitive reactance being switched to the transformer magnetizing reactance, both on a line-to-neutral basis (this may be considered the ratio of transformer magnetizing kilovolt-amperes to capacitance kilovolt-amperes), falls between the approximate limits of 0.0012 and 0.02 or 0.45 and ∞. In the region between 0.02 and 0.45, reasonably simultaneous opening of the three phases is essential to keeping the switching surge voltages low.

3. The prevention of overvoltages of harmful magnitudes, which may occur in certain limited fields on account of the forcing of the current zero by the circuit breaker on interrupting, may be accomplished by the use of shunt capacitors. The minimum capacitor kilovolt-ampere value for satisfactory performance depends upon the characteristics of the circuit, and the breaker, and the allowable overvoltage magnitude.

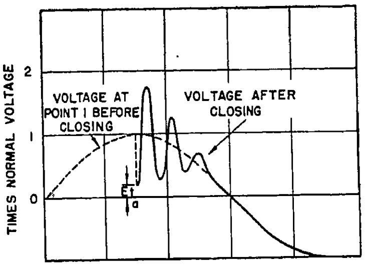
It is not intended to convey the impression that in every case of switching capacitive circuits or of switching magnetizing currents, special means need be used to prevent serious switching surge overvoltage. Although restriking may occur while opening capacitive circuits, such restriking will not necessarily produce overvoltages serious from the standpoint of circuit and apparatus insulation, particularly where adequate lightning-arrester protection is employed. This also is true of overvoltages produced by off-zero magnetizing-current interruption.

Switch-Closing Overvoltages

When an unenergized circuit is suddenly connected to an energized one, a voltage oscillation of twice (neglecting damping caused by losses) the voltage difference between them at the instant before closing occurs immediately following the closing operation. The reason for this is the presence of system capacitance, and it may be illustrated by Figure 1A which shows a one-line diagram of a system, a switch, and a capacitor. Assuming that the switch is closed at the peak of the generated voltage wave, *a*, in figure 1B, the voltage at point 1 instantaneously drops to a value given by


SYSTEM INDUCTANCE
1
2
SOURCE
C_1
SYSTEM CAPACITANCE
C_2
CAPACITOR REPRESENTING LINE OR CABLE CAPACITANCE OR GROUNDED NEUTRAL CAPACITOR BANK

A. Equivalent circuit of system, switch, and a capacitance



B. Voltage transient on closing switch of Figure 1A at normal voltage crest

Figure 1. Illustrating switch-closing transient

$$E_t = \frac{EC_1}{C_1 + C_2}$$

and an oscillation of voltage of amplitude $E - E_t$ then takes place about the fundamental voltages as shown, rapidly decaying, in general, because of losses and other system damping such as loads. The oscillation frequency is determined by the system inductance and total capacitance.

In the limit, if C_1 is reduced to zero, E_t becomes zero, the oscillation amplitude becomes E, and the total voltage on the capacitor reaches $2E$, if the switch is closed at the voltage peak and damping is neglected.

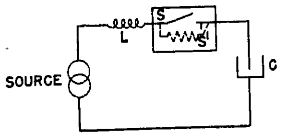
It has been shown[2] that, if the capacitance being switched, C_2, in Figure 1A, is replaced by a line of distributed inductance and capacitance, a similar voltage oscillation will appear all along the line. In the case of the line, however, the oscillation will appear at various points along the line at successive intervals of time and will require a time to reach the far end, depending primarily on the line length and its inductance and capacitance per unit of length. The frequency of the oscillation is also governed by these quantities.

Switch-Opening Overvoltages—Interrupter Restriking

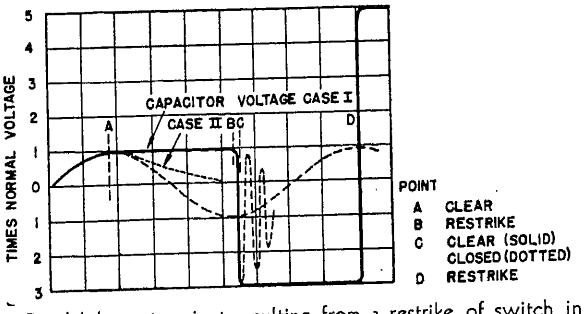
Switch-opening transients are generally negligible if the interrupter opens the circuit at or very near current zero and does not restrike. Restriking takes place in any interrupter if the voltage across its contacts following an interruption recovers more rapidly than the dielectric strength of the insulating medium. When a fault on a solidly grounded system having small capacitance is being cleared by an interrupter, it may restrike several times following an attempted clearing at a current zero, until the contacts have gotten sufficiently far apart that they can withstand the recovery voltage transient of less than twice normal crest line-to-ground voltage, which will occur at each attempted clearing. This restriking is normal and expected and is not harmful either to the breaker or the system. (On a reactance-grounded system, restriking when clearing unbalanced faults can result in severe overvoltages if the ratios of zero-to-positive-sequence inductive reactance are high.[3])

However, under certain conditions, overvoltages of a serious nature may occur as a result of delayed restriking when opening the normally low balanced charging current of capacitance circuits, notably transmission lines and large banks of capacitors. The results of recent studies on capacitor[4] and transmission-line[5] switching, in which this phenomenon has been analyzed, have been reported. For the sake of completeness, a brief discussion of the mechanism by which these overvoltages are obtained follows.

Referring to Figure 2A which represents



A. Equivalent circuit of system, switch, and a capacitance

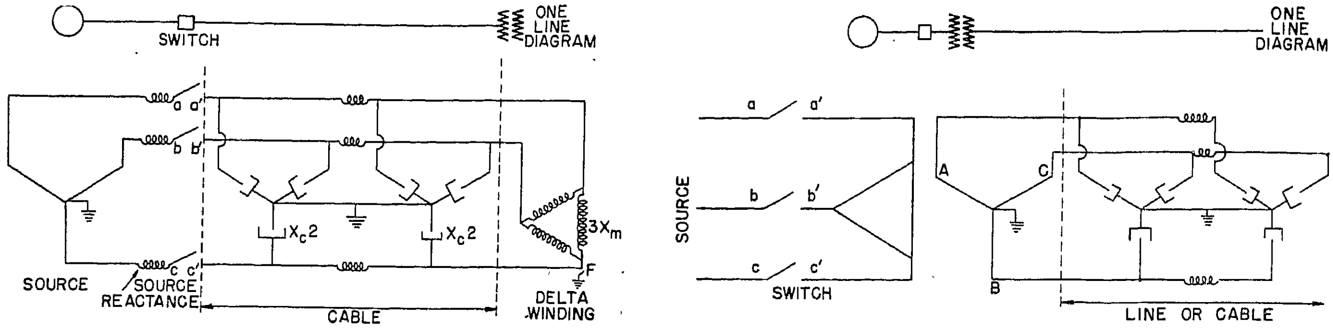


B. Voltage transient resulting from a restrike of switch in Figure 2A 180 electrical degrees after interruption at normal current zero (voltage crest)

Case I—Without resistor across switch

Case II—With resistor across switch, final interruption by opening switch *S'* of Figure 2A

Figure 2. Illustrating transient voltage resulting from restriking when opening a capacitive circuit



A. Three-phase cable circuit with saturable delta winding switched together

B. Three-phase cable circuit with delta–wye-grounded transformer between switch and cable

Figure 3. Miniature-system (transient-analyzer) connection diagrams

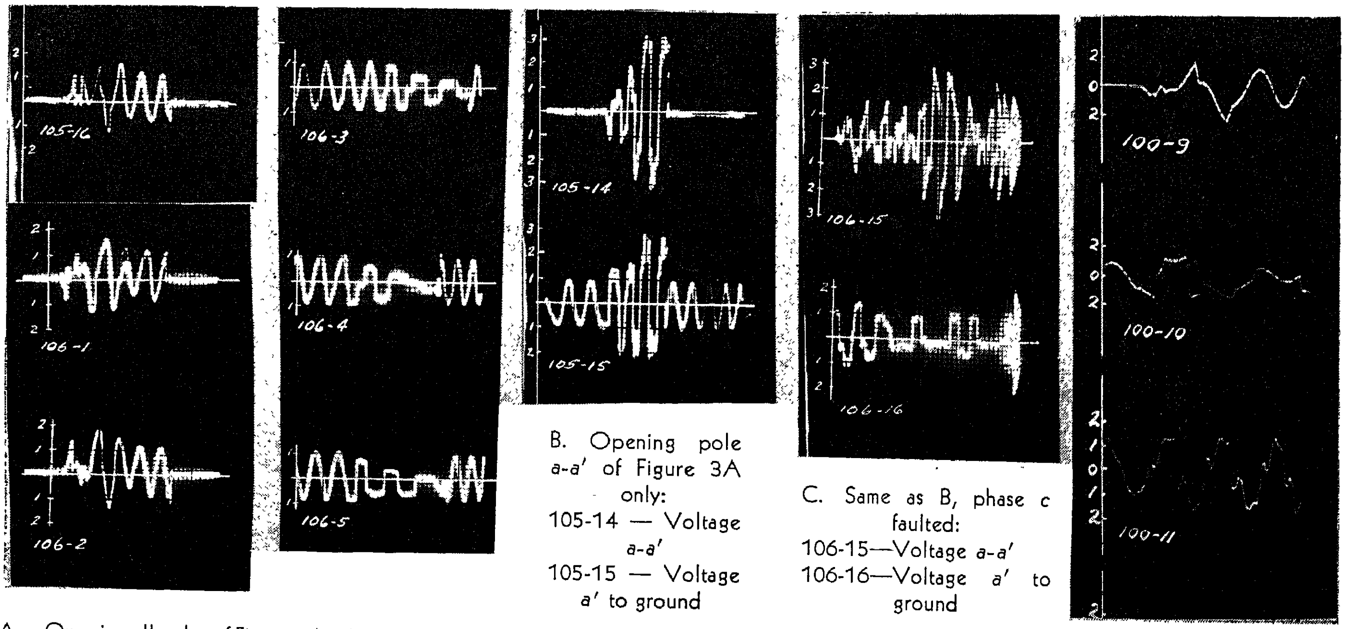
one phase of a system of inductance L supplying a capacitance load, C, let switch S be opened. At a current zero (voltage crest point A of Figure 2B), the current will be interrupted, leaving a full charge on the capacitor. The voltage across the interrupter contacts will grow from zero to twice normal in 180 electrical degrees, as shown by the difference of the stored capacitor charge and the system normal frequency voltage. If restriking occurs during this period, a voltage oscillation will result of magnitude equal to the switch voltage, about the fundamental, resulting in a total voltage of nearly three times normal (neglecting damping) if the restrike occurs when the switch volts are a maximum, as shown at point B. The oscillation will be at natural frequency, and at its first peak (point C), the arc current will be zero. If conditions within the interrupter are conducive to the arc's remaining extinguished at this point, the switch voltage will grow from twice to four times normal in the next 180 degrees, and a restrike at point D would result in a voltage of nearly five times normal. If the arc does not go out at C, the natural frequency voltage will gradually decay as shown by the dashed curve.

It should be remembered that whether the interrupter will restrike under this switching condition depends upon the magnitude of the capacitance current being interrupted and the interrupter characteristics. Tests[4] have established the amount of capacitance, in terms of kilovolt-amperes, that certain breakers in the voltage class 15 kv and below may interrupt with reasonable assurance against delayed restriking. Perhaps future tests will be desirable at higher voltages.

A study of Figure 2B indicates that whatever may be done to reduce the charge stored on the capacitor or line after the first clearing will help very materially, not only to prevent restriking when switching capacitive circuits, but also to reduce greatly the magnitude of the overvoltage obtaining if restriking does occur. The following paragraphs in this section will be devoted to discussing the various means which may be employed to bring about a reduction in the capacitor or line voltage within the half-cycle following the first interruption of the circuit.

1. Resistor Switching

If, when the switch S of Figure 2A is opened, a relatively high resistance is



A. Opening all poles of Figure 3A, ideal clearing:
105-16—Voltage a-a'
106-11—Voltage b-b'
106-2—Voltage c-c'
106-3—Voltage a' to ground
106-4—Voltage b' to ground
106-5—Voltage c' to ground

B. Opening pole a-a' of Figure 3A only:
105-14 — Voltage a-a'
105-15 — Voltage a' to ground

C. Same as B, phase c faulted:
106-15—Voltage a-a'
106-16—Voltage a' to ground

D. Opening pole a-a' of Figure 3B:
100-9 —Volts a-a'
100-10—Volts a' to ground
100-11—Volts C to ground

Figure 4. Transient-analyzer oscillograms

All calibrations in times normal line-to-neutral voltage inserted in the circuit by this action, as would be the case if the interrupter were shunted by a resistor as shown by the dotted lines, the capacitance is afforded a discharge path through sufficient resistance to permit considerable decay of its voltage as shown in Figure 2B, case II (dotted curve). This scheme not only reduces the possible overvoltages but also affords a relatively easy subsequent interruption of the circuit by switch S', because, since the resistor ohms are relatively high, the current will be nearly in phase with the voltage, the current will be small, and at current zero the circuit voltage will be near zero.

Reference 5 describes tests and analyses of an oil circuit breaker provided with such a resistor for reducing the overvoltage hazard when interrupting a long high-voltage line, and reference 2 similarly indicates performance of a breaker provided with resistors to assist in switching a large 13.8-kv capacitor bank. Reference 5 shows that it is possible to obtain a practical value of contact shunting resistance which will provide a very satisfactory means of solving the capacitance switching problem. It should not be inferred, however, that all existing breakers for switching long high-voltage lines need be provided with resistors.

2. Discharge Through Transformer Windings

In the case where, after the switch is opened, the capacitor or line is left with a transformer bank in shunt with it, the windings provide a discharge path for the energy stored in the capacitance. The effectiveness of the windings depends upon their size and whether they are delta—or grounded-wye—connected to the line.

(*a*). *Delta or Ungrounded-Wye Windings.* Refer to Figure 3A. This shows the connection diagram of a miniature-system representation of a cable having a total equivalent shunt capacitive reactance per phase of X_c ohms with the delta winding of an unloaded transformer connected to it. The transformer has a magnetizing reactance per phase of X_m on a line-to-neutral basis. As the first pole of the interrupter *a-a'* opens, the cable capacitance begins to discharge through the transformer, thus reducing the voltage across the switch initially, over the case of the cable or line alone. If the remaining poles of the interrupter subsequently clear "ideally"—each at its nearest available current zero, the switch voltage (across *a-a'*) will never reach twice normal, and the tendency of the interrupter to restrike should be markedly reduced.

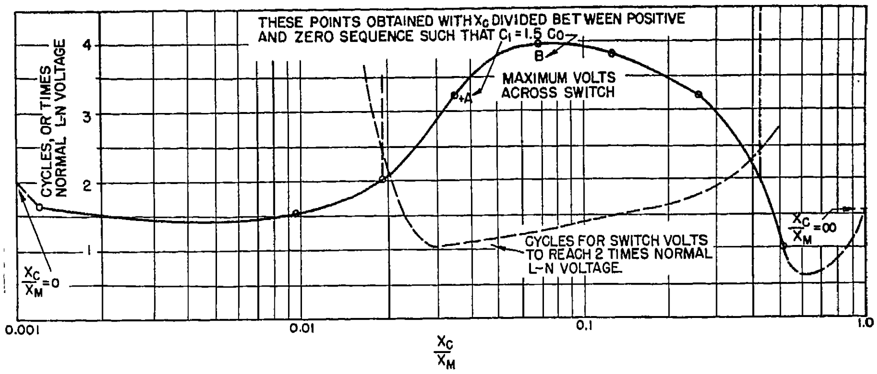


Figure 5. Summary of maximum switch voltages and time for switch voltage to reach twice normal line-to-neutral voltage when opening switch a-a' of Figure 3A for various values of X_c/X_m

Oscillograms 105-16 through 106-5 of Figure 4 show switch and cable voltages for the case of ideal clearing for a ratio of $X_c/X_m = 0.052$. If the interval of time between successive pole clearings is too large, however, the switch and cable voltages may, depending upon the ratio X_c/X_m, become much larger than twice normal, because of series resonance between the capacitance of the unopened conductors and the saturable transformer reactance. These are the overvoltages mentioned previously and reported in reference 1. The time required for the switch voltage to reach twice normal again depends on the ratio X_c/X_m and is summarized for the first pole to clear in curve form, Figure 5, together with the steady-state values that the switch voltage will attain for various values of the ratios. Oscillogram 105-14 shows the voltage across the first pole (*a-a'*) opening with the others remaining closed for a ratio of $X_c/X_m = 0.052$. Oscillogram 105-15 gives the voltage of point *a'* to ground for this condition of interruption. It will be noted that it required about one cycle for voltage *a-a'* to reach twice normal line-to-neutral value, and, if reasonably simultaneous clearing of the other poles had occurred, pole *a* would have had a comparatively low voltage across it while opening. On the basis that similar results were obtained on the other poles, it was considered adequate to use the transient resulting from the first pole to open as a general criterion of the effectiveness of a delta winding to prevent the switch voltage from reaching at least twice normal in a short time. Also, only a general indication of results was necessary, since in general long times between successive pole openings cannot be tolerated.

It was interesting to note that, if a ground fault on one of the cable or capacitor phases as shown by *F* in figure 3A was being cleared, the transient voltages resulting from clearing the first of the unfaulted poles are somewhat reduced from the case of no fault. The reason for this is that a discharge path for the capacitance in addition to the delta winding exists by virtue of the fault. Oscillogram 106-15 shows the switch voltage (*a-a'*) for $X_c/X_m = 0.052$ for the condition of phase *a* being cleared, with a fault on phase *c*, and 106-16 shows the voltage of *a'* to ground for this condition.

The data in Figure 5 is for the condition of equal positive- and zero-sequence capacitances. To show the effect of a ratio of positive-to-zero-sequence capacitance of 3/2 (approximately the value for an overhead line), tests were made on the miniature system which indicated the effect to be one of moving the curves of Figure 5 slightly to the right.

(*b*). *Grounded-Wye Windings.* If the line or grounded-neutral capacitor is left with a grounded winding instead of an ungrounded one, it will be somewhat more effective than an ungrounded one, because it is capable of discharging both zero- and positive-sequence capacitance. A special case where a delta–wye-grounded transformer is interposed between the interrupter and line with the line or capacitor connected to the wye-grounded winding was investigated. Figure 3B shows the miniature-system connections. The case of a transformer large enough to carry the line-charging or capacitor kilovolt-amperes being interrupted alone resulted in voltages of less than twice normal from line to ground even with a restrike applied at a time when the switch voltage following first clearing was a maximum. Oscillograms 100-9 to -11 show miniature-system voltages for this case. This indicates the benefits to be gained by "low-side switching" of lines, cables, or capacitors.

Switch-Opening Overvoltages—Interruption Off Current Zero

Another means by which an overvoltage may be obtained on opening a circuit is the studden stoppage of current flow before a normal-frequency current zero has arrived. Under this condition, the voltage obtainable is controlled by the circuit inductance and the rate of change of current caused by the interrupter thus forcing current zero. Overvoltages of this nature have been encountered in the process of interrupting transformer magnetizing currents which are small in magnitude, compared to the normal load current of both the transformer and the breaker. In particular, this problem has been encountered in connection with arc-furnace transformers which, because they are switched at no load frequently, are more apt to be subjected to this type of switching surge than other transformers, other things being equal.

Here again the tendency for the overvoltage to occur is dependent upon the characteristics of the interrupter at the magnitude of current to be switched. For a particular value of current a given interrupter might break the circuit at current peak (worst case), at or near zero, or anywhere in between.

Lightning arresters can be and have been used to protect transformers from this type of overvoltage, but it should be recognized that, if the interrupter cuts off at too high a current, the energy (LI^2) stored in the transformer may be greater than the arrester can dissipate without damage to itself.

An obvious means of preventing the possibility of this type of switching-surge overvoltage is to supply the magnetizing kilovolt-amperes of the transformer by means of capacitors, and switching the two together. The only no-load current to be switched if complete compensation is provided is loss current, which will be very small. It is not particularly important in this method whether either the transformer winding or the capacitor neutral is grounded.

Analysis (see appendix) and miniature-system tests indicate that it is not necessary to supply entirely the transformer magnetizing kilovolt-amperes in order to limit the transient voltage to acceptable values. In the appendix it is shown that for the circuit of Figure 6, representing a transformer winding protected by a capacitor, assuming interruption at current crest (worst case) gives a voltage oscillation across the transformer–capacitor combination whose equation is

$$E_p = -E\sqrt{\frac{X_c}{X_m}} \sin \left\{ \sqrt{\frac{X_c}{X_m}} t \right\}$$

where

E = normal line-to-neutral voltage

X_c = capacitor line-to-neutral ohms per phase at fundamental frequency

X_m = transformer magnetizing line-to-neutral ohms per phase at fundamental frequency and normal voltage

The expression $\sqrt{X_c/X_m}$ may be written

$$\sqrt{\frac{\text{transformer magnetizing kilovolt-amperes}}{\text{capacitor kilovolt-amperes}}}$$

and gives the crest value of the voltage oscillation following interruption and the order of its natural frequency. Thus a capacitor kilovolt-ampere of 25 per cent of the transformer magnetizing kilovolt-amperes will limit the overvoltage under the worst condition of interruption, at current crest, to twice normal, and the oscillation will be at second harmonic frequency.

Although this analysis is made on the basis of an equivalent single-phase circuit with saturation of the transformer neglected, miniature-system tests on a three-phase setup including saturation indicates that the results obtained apply practically whether the transformer and/or the protective capacitors are grounded or ungrounded.

Appendix—Magnetizing-Current Interruption Off Zero

In the equivalent circuit of Figure 6, in which a shunt capacitor is connected to the terminals of the transformer, let

Per unit transformer magnetizing kilovolt-amperes $= \text{kva}_m = \frac{1}{X_m}$

Per unit capacitor kilovolt-amperes $= \text{kva}_c = \frac{1}{X_c}$

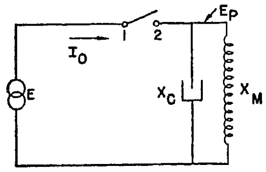


Figure 6. Equivalent circuit of a source, a switch, and an unloaded transformer shunted by a protective capacitor

The current flowing before the switch is opened is

$$I_0 = \frac{E(X_c - X_m)}{X_c X_m}$$

Assuming interruption at current crest (worst case), this may be represented mathematically by inserting the negative of this current through the switch, letting $t=0$ at current crest and the switch voltage will be $I_c \cos \omega t Z(p) = E_{12}$. The voltage across the transformer–capacitor combination is

$$E_p = E + E_{12}$$
$$= E \sin \omega t + I_0 \cos \omega t Z(p)$$

in operational form this is, where

$$\omega_0^2 = \frac{1}{CL}$$

$$E_p = \frac{E\omega p}{p^2+\omega^2} 1 + I_0 \frac{\omega_0^2 p^2}{(p^2+\omega^2)} \times \frac{pL}{(p^2+\omega_0^2)} 1$$

And the solution of this is

$$E_p = E \sin \omega t - \frac{I_0 L \omega_0^2 \omega}{(\omega_0^2 - \omega^2)} \left[\sin \omega t - \frac{\omega_0}{\omega} \sin \omega_0 t \right]$$

which reduces to

$$E_p = -E\sqrt{\frac{X_c}{X_m}} \sin \left(\sqrt{\frac{X_c}{X_m}} t \right)$$

References

1. ABNORMAL VOLTAGE CONDITIONS IN THREE-PHASE SYSTEMS PRODUCED BY SINGLE-PHASE SWITCHING, **Edith Clarke, H. A. Peterson, P. H. Light.** AIEE TRANSACTIONS, volume 60, 1941, pages 329–39.

2. Discussion by **T. W. Schroeder** of reference 5.

3. EFFECT OF RESTRIKING ON RECOVERY VOLTAGE, **C. Concordia, W. F. Skeats.** AIEE Transactions volume 58, 1939, August section, pages 371–5.

4. TESTS AND ANALYSIS OF CIRCUIT-BREAKER PERFORMANCE WHEN SWITCHING LARGE CAPACITOR BANKS, **T. W. Schroeder, E. W. Boehne, J. W. Butler.** AIEE TRANSACTIONS, volume 61, 1942, November section, page 821.

5. SWITCHING OVERVOLTAGE HAZARD ELIMINATED IN HIGH-VOLTAGE OIL CIRCUIT BREAKERS, **Lloyd F. Hunt, E. W. Boehne, H. A. Peterson.** AIEE TRANSACTIONS, volume 62, 1943, February section, page 98.

Automatic Voltage Compensator for Resistance Welding Control

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AMONG the more recent developments in a-c resistance welding control is the voltage compensator. This compensator is an auxiliary control used in conjunction with a standard type of phase-controlled welding panel, the circuit of which is shown in Figure 1. The function of the voltage compensator is to maintain the weld current substantially constant irrespective of line-voltage variations. The inability to obtain new feeders and the burden of added loads in war plants have in many cases made the power systems incapable of supplying several welding machines simultaneously without experiencing an appreciable drop in line voltage. Welding heat, which varies with current squared, will be reduced by 19 per cent for a ten per cent drop in line voltage; a line drop of 20 per cent will reduce the welding heat 36 per cent which is intolerable for consistent welding. These conditions can be remedied by the addition of a small inexpensive auxiliary control, such as the voltage compensator. The regulation of many overworked high reactance welding feeders can then be tolerated because consistent welds will be produced, whereas without proper compensation, poor welds would inevitably result. A compensator unit is shown in Figure 2 installed just in front of the current transformer in the bottom of a portable welding-machine control panel.

Requirements of the Compensator

First, let us consider where the compensator is required. Small voltage variations are permissible, since welding pressure, electrode shape, metallic finishes and other conditions may vary to some degree. Information in previous papers indicates that a five per cent voltage variation will not seriously reduce the weld strength of many types of steel.[1,2] Heavier line drops will probably require the voltage compensator. Its chief utility is on lines having more than one welding machine, since the line drop can be taken into account when adjusting current for one machine. Where line voltage drifts up and down, the compensator becomes advantageous with just one machine.

The next requirement is that the control operate entirely automatically. In operation, any gradual drift of the power-supply voltage will be continuously corrected, so that upon initiation of a spot weld, the initial cycles automatically start at the corrected heat setting. It is also important that the transient rate of response to sudden voltage changes should be as rapid as possible. This rapid response is very necessary in adapting such a control to seam and short-timed spot welding, where a slow response would result in relatively large variations between spots. It is, therefore, equally important that this voltage compensator be capable of nullifying the effects of transient as well as gradual power-supply fluctuations.

Other desirable features include simplicity, ease of connecting and adjustment, and, finally, low cost.

Phase-Controlled Welding Circuits

In order to understand how this voltage compensator works, it will be advantageous to review a standard phase-controlled welding circuit. Common to all electronic a-c welding control circuits are two power tubes, usually of the ignitron type, which are connected in an inverse parallel or back-to-back arrangement as shown in Figure 3. The function of these tubes is to serve as an electronic switch in controlling the power delivered to the welding machine. In this two-tube arrangement a-c power may be passed, since one tube may be rendered conductive in one direction, while the other tube, connected in reverse manner, permits conduction in the other direction. To render an ignitron conductive, a current must be passed through its ignitor or control element. This firing current is furnished by a thyratron-type tube which is capable of delivering high currents for short intervals. The welding control is designed to control these firing tubes, which in turn render the power tubes conductive.

When phase control is added to this electronic switch, the point at which this conduction is initiated is varied so that the power tubes carry current for a given preset portion of each half cycle. The particular phase-control circuit to which the line-voltage compensator was applied is shown in an elementary manner in Figure 1. For simplicity, all grid to cathode capacitors and transient arresting resistors have been left out. The net grid bias voltage on the firing tubes (see Figures 3 and 4) is the sum of four separate voltages:

1. An a-c "hold-off" bias of 300 volts supplied by transformer *T*1.
2. An a-c "turn-on" bias of 220 volts supplied by grid transformer *T*3.
3. A peaked triggering voltage of approximately 200 volts peak from transformer *T*2.
4. A self-rectified d-c voltage across capacitors *C*3 and *C*4 which appears there as a result of grid current flowing during the time the grid was positive.

Normally transformer *T*3 is de-energized, rendering the firing tubes nonconducting by virtue of the negative grid bias throughout the period of positive plate voltage as shown in Figure 4*A*. Upon energizing grid transformer *T*3 (Figure 4*B*) the net a-c bias is reduced such that the peak of firing voltage from transformer *T*2 exceeds the critical grid voltage of the firing tubes, permitting them to be conductive and thereby initiating the power tubes. For accurate and synchronous timing, transformer *T*3 is energized from a thyratron timing circuit, but the time could be controlled by a simple pushbutton hand switch. The phase setting of the *T*2 firing peak is determined by a resistance-capacitor bridge in which the resistance element *R*7 is adjustable for changing the phase of the peaked firing voltage. The components in series with the primary winding of the peaking transformer *T*2 form a filter to insure only one positive firing peak each half cycle. This circuit is resonant so that voltage and current are approximately in phase. However, the peaked voltage of *T*2*S* which is about 10 degrees wide occurs at current zero and is therefore 90 degrees out of phase with the phase-shifting voltage vector.

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The compensator may be adapted to any standard panel by three connections, 6, $7B$ (reconnection from 7), and 8 as shown in Figure 1.

Operation of the Compensator

The fundamental purpose of this compensator is to advance automatically the phase of the "peaker" firing voltages when the line voltage drops, and vice versa, to retard the phase should the line voltage increase. This is done by replacing the fixed transformer side of the resistance-capacitor phase-shift bridge with a voltage-dividing network which will vary in accordance with changes in line voltage. Referring to the elementary diagram of the compensator, Figure 5, the voltage divider consists of the primary winding of transformer $T101$ and resistor $R101$. Transformer $T101$ is made to act as a variable resistance by the action of the shorting tubes 102 and 103. The grid voltage of tubes 102 and 103 when varied causes the tubes to act as a variable resistance load in the secondary winding of transformer $T101$ and makes $T101$ appear substantially as a resistance in the voltage divider. The center point, $7B$, of the voltage divider may be shifted back and forth by some magnitude e_c as shown in Figure 6. This causes a shift in the vector relation between vector 6–8 which corresponds to the phase of the line voltage, and vector $7B$–9 which in turn determines the phase of the peaked firing voltage $T2S$ of transformer $T2$ with respect to the line voltage. When the grids of tubes 102 and 103 are made less negative, tubes 102 and 103 appear as a lower resistance load and hence cause point $7B$ to move toward point 8, advancing the phase of the firing voltage vector $T2S$ with relation to vector 6–8. In a reverse manner the phase of the firing peak may be retarded by making the grids of tubes 102 and 103 more negative.

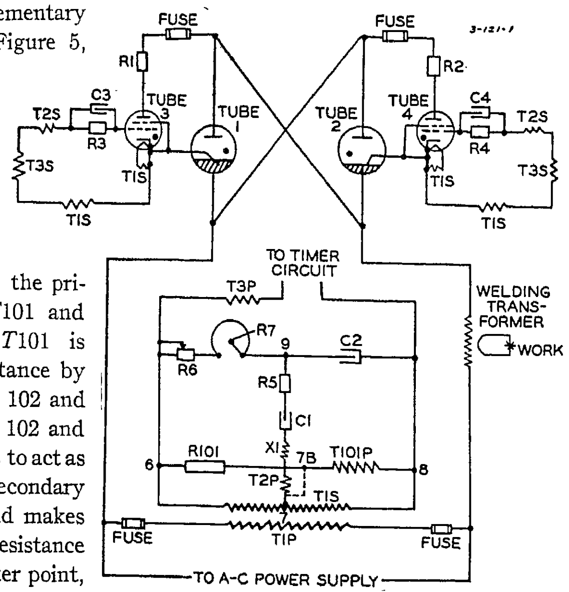

Figure 1. Simplified diagram of phase-controlled welding circuit

To obtain the operating grid voltage, transformer $T102$ is connected directly across the line supply and its output voltage rectified by tube 101. By making time constants of the filter on the output of the rectifier voltage relatively low, this d-c voltage can be made to follow closely all fluctuations in the line supply. D-c output of the rectifier provides a reference voltage across the voltage regulating bridge (tubes 104 and 105), which at normal line condition give zero voltage across points 20 and 21. A d-c signal voltage directly proportional to the line-voltage change appears across these bridge output terminals.

It is necessary next to investigate the amount of compensation or phase shift required to maintain the welding current constant for a given line-voltage drop at various power factors of the welding circuit and per cent current settings. The higher the power factor, the greater will be the phase shift required; and likewise, the larger the percentage of full-phase current flowing, the greater will be the shift required to maintain the current. Figure 7 illustrates the approximate phase shift requirements to correct for a 10 per cent line-voltage drop for various power factors and per cent current settings. It should be apparent that once full-phase firing has been reached, no additional compensation can be obtained. Hence, if the line voltage falls to 90 per cent of its normal value, a maximum (depending on power factor) of approximately 95 per cent current can normally be delivered to the welding machine. The compensated timer requires a five per cent, and in some cases ten per cent, zero current gap to allow for high heat compensation by phase advance. It is, however, usual practice to include this slight zero current gap at full heat in most welding machines so that any changes in secondary power factor will not permit heat-control settings to range above 100 per cent sine wave current. Unstable operation might result in operating practice unless this slight open current gap was adjusted for the ordinary welder control panel.

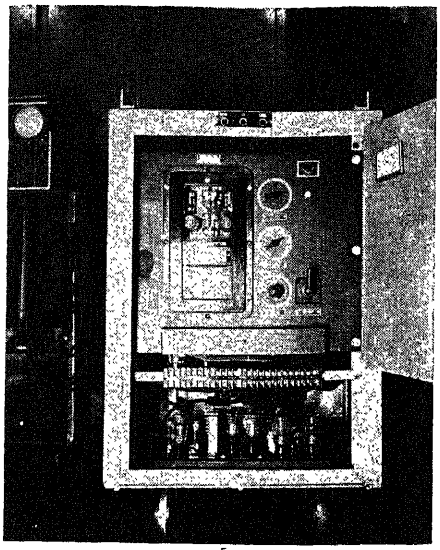

Figure 2. Panel with I^2T recorder and compensator installed

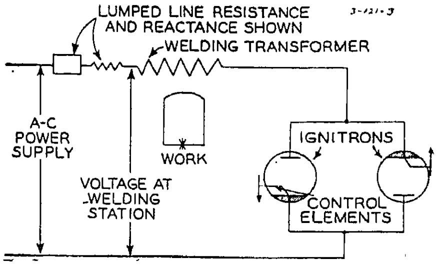

Figure 3. Typical welding power circuit

Figure 8 shows how the setting of $P101$ is used to obtain a greater rate of compensation at higher than normal power factors. $P101$ is adjusted so that there is an unbalance as much as 20 per cent between the voltages 6–$7B$ and $7B$–8. When a voltage drop occurs, point $7B$ is moved toward 8 by the amount e_c. This produces an angular advance in the trigger firing voltage of a_1, a_2, and a_3 for high, intermediate, and low heat settings. For the range of 20 per cent to 60 per cent power factor, which covers most resistance welding setups, a_1 is substantially greater than a_2 and a_3 without too much unbalance between voltages 6–$7B$ and $7B$–8. Thus $P101$ is adjusted to obtain a curving characteristic of phase shift versus heat setting. $P101$ is used as a bridge sensitivity control and therefore acts to determine the slope of the characteristic curve. By correlating the two adjustments it is possible to obtain a compensator characteristic which closely approaches the requirements shown by Figure 7. That this has been accomplished is borne out by experimental results which follow.

Data on Operating Performance

Referring to Figure 7, it is seen that the compensator must respond over a

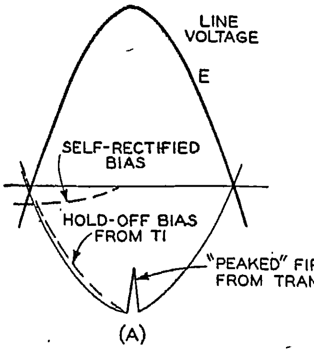

Figure 4A. One firing tube control voltage—standby

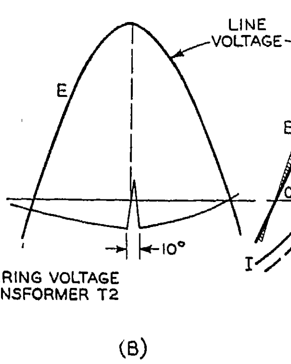

Figure 4B. Same firing tube voltage—for welding. Resultant grid firing voltage shown

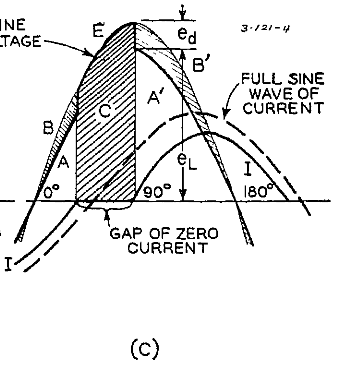

Figure 4C. One half-cycle welding voltage and current showing instantaneous line drop

wide range of phase-angle advance for a variety of conditions that depend primarily on heat current setting and power factor. It must also be taken into account that the regulation may vary considerably, depending on the number of other machines that "hit" simultaneously. Differing portions may also be "chopped" off the useful area of the line-voltage wave as governed by the fractional heat dial settings of these machines. Then, too, interfering welders may also "come and go" early or late during a weld period. Fortunately, the control bridge circuit is very sensitive and responds correspondingly to all degrees of regulation, handling instantaneous line drops readily.

It would be well to consider briefly the instantaneous voltage drop imposed on a welding station by power transformer and feeder reactance. Standardization has not yet fully clarified the meaning of percentage voltage regulation for phase-controlled welding machines caused by this effect. From Figure 4C it should become apparent that with the form of voltage wave shape shown, the use of a voltmeter to register welding load voltage is inaccurate. The cathode-ray oscilloscope offers a better means of obtaining the voltage drop. The question then arises as to where to measure the voltage drop when speaking in terms of percentages. One criterion might be to consider the regulation as:

$$\%\text{ instantaneous regulation} = \frac{e_d}{e_L}\text{ at line-voltage peak (90 degrees)} \qquad (1)$$

Here e_d and e_L are initial line drop and load voltage respectively. Also:

$$e_d = L\,di/dt \text{ for } i_0 \qquad (2)$$

where L = lumped power source inductance

In considering over-all welding regulation, it is necessary to discard the no-load voltage area C shown in Figure 4C. Regulation for the half cycle shown then becomes:

$$\%\text{ total regulation} = \frac{\text{area } B+B'}{A+A'+B+B'} \qquad (3)$$

The voltmeter could measure this accurately for one condition, namely, full sine wave welding current, where an instantaneous drop does not normally occur. Strictly speaking, equations 1 and 3 must be reconsidered each time another fractional heat current is used. The reason for this is that partial heat currents are nonsinusoidal and have initial rates of rise that depend on the ignition point, that is, the controlled firing angle. The welding power factor also contributes to the degree of the instantaneous voltage drop. Thus the importance of instantaneous voltage drop cannot be overemphasized.

The detection circuit of the voltage compensator was designed to take into account this complication of measuring just such voltage regulation. The rectifier supply, which is in effect the detection circuit, has an inductive input filter which integrates the voltage wave, so that instantaneous drops that occur after 90 degrees will readily be averaged into the resultant output voltage.

A test was conducted using actual weld pull specimens and dropping the voltage by means of another timer operated simultaneously. The results with and without compensation are shown in Table I. Note that the average for 0.016-inch stainless steel with compensation is 399 pounds pull strength, whereas when uncompensated the average fell to 333 pounds strength because of external line drop. An ampere-squared second recorder indicated that approximately only 70 per cent heat was getting to the weld when uncompensated.[3] The difference is even more marked for 0.050-inch stainless steel, shown in Table II. Here the average fell from 2,634 pounds pull when compensated down to 2,063 pounds pull when not compensated for the particular line drop. The recorder indicated approximately 76 per cent heat into weld for the uncompensated case. A definite advantage in favor of the compensator was found as a result of these tests.

The compensator was next tested under severe conditions and operated with an instantaneous voltage drop as high as 27 per cent. Without compensation the recorder indicated that the heat into weld was about 50 per cent of normal. The compensator was therefore able to bring the heat up to 100 per cent (entirely back to normal). This is strikingly illustrated by shear strength pull tests which showed perfect weld strength of 2,300 pounds when compensated but fell off to 1,350 pounds when uncompensated for the drop referred to. Such a test points to suitable compensation for several heavy welders operating simultaneously, provided the power source is not being too severely overloaded. The main purpose of this test was to determine evenness of compensation under the wide variety of conditions mentioned earlier. With

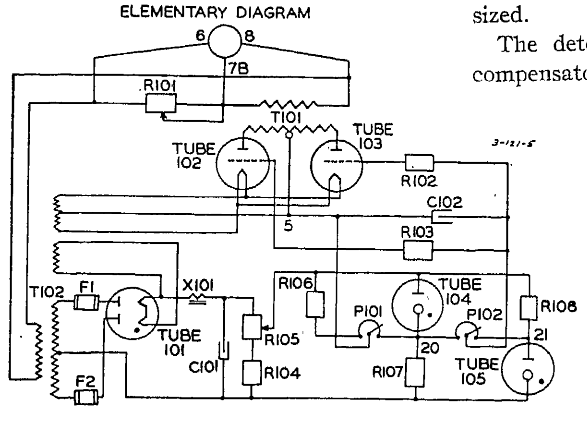

Figure 5 (left). Elementary diagram of compensator panel

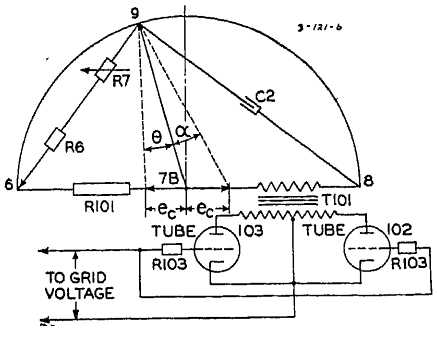

Figure 6 (right). Vector sketch showing how phase of peaked firing voltage is shifted by compensation

Heat control circuit is included. e_c is compensation voltage

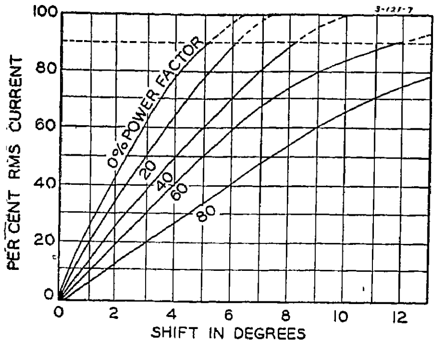

Figure 7. Phase shift required to maintain constant current with ten per cent line-voltage drop

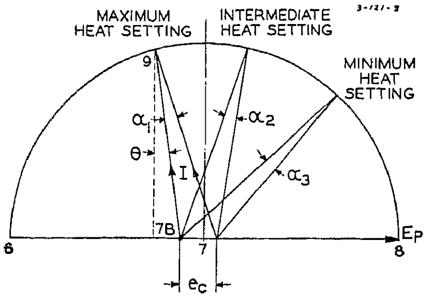

Figure 8. Vector sketch showing heat control range and point *7B* shifted as required

various combinations of heavy and light loading for operation of both timers, deviations of the recorder for the most part hovered well within five per cent of normal. Adjustment of the compensator was not changed during this test. The greatest deviation experienced was over compensation of 15 per cent with the extremely heavy external drop referred to and then only for very low heat settings of the compensated timer. Even this is within tolerance limits and is remedied by reducing the power-factor-gain control slightly, if machine is operated continuously on this range. The power factor used was 0.6, which is fairly high, and the time was eight cycles. Consistent results were also obtained on lower power factors.

Oscillograms were taken to illustrate compensator action. In order to provide the line drop, a second timer known as a line dropping timer was operated on the same line. Referring to Figure 9, the two timers came on the line together, and the compensator-test timer had the compensator-gain-control knob *P*102 turned off. Thus, the test timer operated without compensation and when the dropping timer *C* stopped, the difference in the magnitude of weld current *A* is apparent.

Figure 10 illustrates normal compensation. The compensator acted fully on the weld current *A* within two cycles from the initial cycle dropping the line. After the external drop went off, the compensator required two cycles to phase back to normal, as seen by the amplitude of cycles 5 and 6 which are above normal. Since these oscillograms were taken, the speed of operation has been reduced to one cycle to yield full compensation. Notice the instantaneous drop expressed by the line voltage *B*. This same voltage drop (Ldi/dt) is experienced by every control transformer and circuit in the control panel and is seen by the dip of *T*101 (primary) voltage *D* of compensator and its related branch *R*101 shown as voltage *E* (reduced for clarity).

A better comparison is obtained from Figures 11 and 12. Figure 11 is without compensation and illustrates the current decrease from a normal rms value of 750 amperes, as read on the primary side of welding transformer. Figure 12 shows how this decrease was made up by compensation. Primary weld current *A* at cycle 2 became lower from an external load, but cycle 3 is almost up to normal compensation. Cycles 4 and 5 (and all further succeeding compensated cycles) appear normal. However, after release from the external load, cycles 6 and 7 continued to respond to phase advance. This is not particularly a disadvantage, as it tends to make up for the early partially compensated cycles (2 and 3). Cycles 8, 9, and 10 duplicate the initial cycle. It can be noticed that the shoulders, or phase concurrence for cycles 8, 9, and 10 are later than for compensated waves 3 to 7 inclusive. This accords with the principle of compensation employed. Were the oscillograph element *A* as responsive as element *C*, these shoulders would have the same flatness. Of importance, the peak amplitude of compensated current loops 4 and 5 need not equal normal loops 8, 9, and 10, since the criterion for equal compensation is that the rms value of the loop areas be equal in both cases. The instantaneous values of *D* and

Table I. Sample Shear Pull Strength Tests for Type 304 Stainless Steel 0.016 Inch Thick

Line Condition During Weld	I^2T Recorder in Per Cent of Normal Heat: With Compensator	I^2T Recorder in Per Cent of Normal Heat: Without	Corresponding Weld Pull (Pounds): With Compensator	Corresponding Weld Pull (Pounds): Without
Compensated weld machine. No line drop (start of test)	100 (Reference)	100 (Reference)	380	397
Compensated weld machine. No line drop (finish of test)	100 (Reference)	100 (Reference)	390	400
External line drop	95	71	418	332
External line drop	100	70	380	342
External line drop	100	70	400	328
External line drop	102	71	412	325
External line drop	100	72	385	340
External line drop	97	73	370	350
External line drop	100	71	375	322
External line drop	101	72	448	320
Average	99.3	71.3	398	332

Type 304 stainless steel 0.016 inch thick—1/4 hard

Weld time: four cycles. Weld current: 70 per cent (fractional). Electrode size: 1/4 inch. Electrode force: 340 pounds total. Line: 240 volts a-c. Regulation: six per cent. (Read with voltmeter during welding current, but using longer time.)

Regulation for instantaneous external line drop: 10 per cent. (Read with oscilloscope for partial current firing at approximately 100 degrees on voltage wave.)

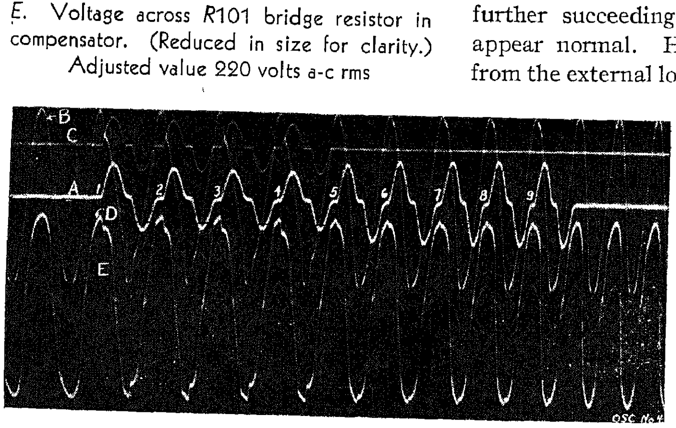

Figure 9. Oscillogram of timer performance without compensation

A. Primary current of welding machine with installed compensator

B. Line voltage (240 volts a-c) common to both welding machines

C. Primary current of line-voltage dropping time

D. Voltage across *T*101 transformer primary winding in compensator. Adjusted value 260-volts a-c rms

E. Voltage across *R*101 bridge resistor in compensator. (Reduced in size for clarity.) Adjusted value 220 volts a-c rms

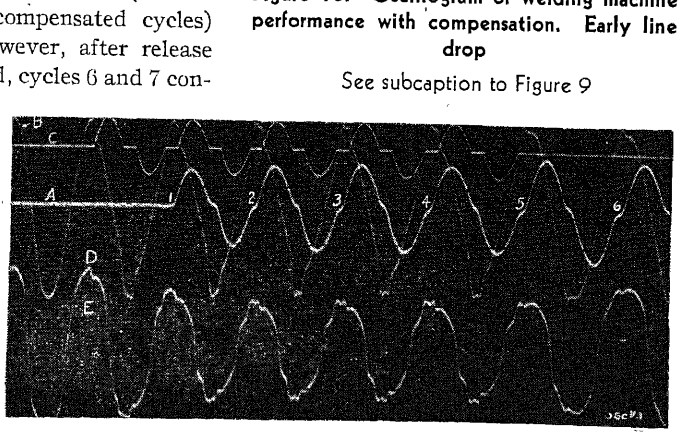

Figure 10. Oscillogram of welding machine performance with compensation. Early line drop

See subcaption to Figure 9

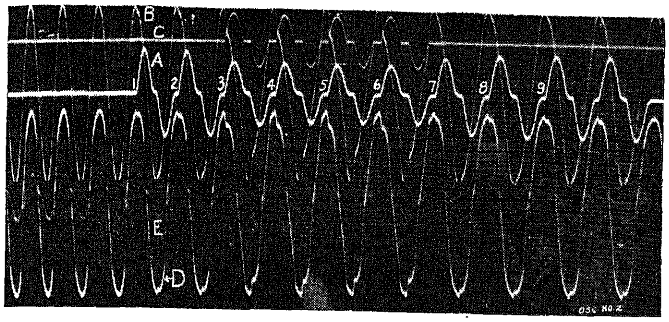

Figure 11. Oscillogram of welding machine performance without compensation. Late line drop

See subcaption to Figure 9

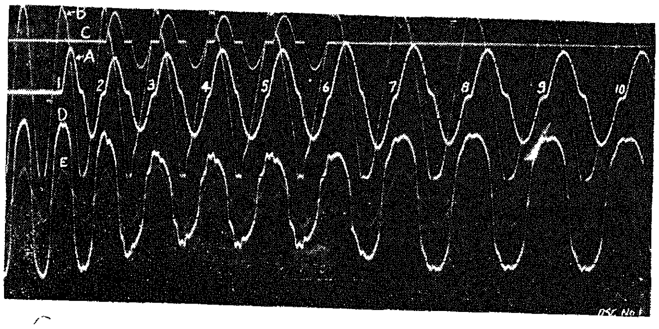

Figure 12. Oscillogram of welding machine performance with compensation. Late line drop

See subcaption to Figure 9

E found in the oscillogram determine the shift of midpoint 7 of Figure 7. Voltage D corresponds to T101 voltage and E to R101, and their shift toward the right for compensation upward agrees with previous discussion. The I^2T recorder read 100 per cent normal heat for the oscillograms being compensated and dropped more than 15 per cent for those which were uncompensated, thereby causing lockout of the welding machine.

Although data were not taken for slow drifts in the sine wave value of line voltage, the response on seam welders using thermocouple ammeter indication has shown that the compensator handles such changes readily.

Investigating Compensator Performance

Several methods of investigating performance are available.

1. The cathode-ray oscilloscope, when connected to read one full cycle of weld current, will indicate the phase-angle advance during compensation. The oscilloscope is also helpful for making electronic control-panel power-factor heat adjustment after installing a compensator.

2. The I^2t recorder, illustrated in Figure 4, is well adapted to record changes in welding current. Where this recorder is available, a quick test is performed by operating two welding machines on the same line individually and together. Uniform recorder response, when the control panel is set to various heat positions, does indicate correct compensation where the regulation would otherwise drop the recorder below the 15 per cent heat limit and cause bad welding.

3. Spot-welding machines may be tested with either clamp-on pointer stop ammeter or pointer stop ammeters in conjunction with current transformers. The previous method for operating two welding machines together is recommended.

4. Seam welding machines may take advantage of a suitable ammeter such as a thermocouple type with an adjustable primary current transformer for indication of operation. Adjustment of the compensator is readily accomplished here, provided a definite line disturbance can be initiated during the test.

5. Where none of the aforementioned methods is available, the compensator can be adjusted and its operation checked by using merely a voltmeter across the control signal bridge and also across the primary winding of T101 for visually checking voltage swings. Normal adjustments once made need not be disturbed following one of these performance checks. The only special adjustments are

(*a*). The degree of compensation desired (same for all normal operation).

(*b*). A slight unbalance of voltages across bridge arms R101 and T101 to allow uniform response in installations where the welder power factor is much higher than normally experienced. All adjustments are quickly performed, using a combination voltage test set.

Table II. Sample Shear Pull Strength Tests for Type 304 Stainless Steel 0.050 Inch Thick

	I^2T Recorder in Per Cent of Normal Heat		Corresponding Weld Pull (Pounds)	
Line Condition During Weld	With Compensator	Without	With Compensator	Without
Compensated weld machine. No line drop (start of test)	100 (Reference)	100 (Reference)	2,700	2,800
Compensated weld machine. No line drop (finish of test)	100 (Reference)	100 (Reference)	2,600	3,100
External line drop	96.5	82	2,725	2,200
External line drop	97	83	2,725	2,400
External line drop	105	70	2,600	1,900
External line drop	100	75	2,625	2,000
External line drop	100	73	2,500	1,800
External line drop	95	73	2,600	1,800
External line drop	95	72	2,700	1,850
External line drop	100	76	2,750	2,100
Average	98.6	75.5	2,634	2,063

Type 304 stainless steel 0.050 inch thick—$^1/_4$ hard.

Weld time: nine cycles. Weld current: 75 per cent (fractional). Electrode size: $^3/_8$ inch. Electrode force: 1,200 pounds total. Line: 240 volts a-c. Regulation: 10 per cent. (Read with voltmeter during welding current, but using longer time.)

Regulation for instantaneous external line drop: 15 per cent. (Read with oscilloscope for partial current firing at approximately 90 degrees on voltage wave.)

Conclusions

1. This voltage compensator helps to maintain uniform current for resistance welding despite the disturbing influence of other machines on the line feeder. With wartime urgency of using existing feeders and with limited use of copper, the problem of poor voltage regulation is eased considerably.

2. Instantaneous reactance line drops (chopped voltage waves) in the order of 30 per cent have been compensated for 100 per cent. Without compensation the heat output was otherwise 50 per cent of normal value.

3. Another feature is compensation for upward or downward line-voltage shifts during the working day.

4. The ability to make consistent welds on poorly regulated lines, is, of course, the chief advantage of the compensator. On installations where the weld recorder locks out the equipment when the value of I^2T swings out of the allowable range, the operator is forced to reset the welding machine and usually to make new weld tests and rewelds. The voltage compensator eliminates to a large degree the number of such lockouts caused by variations in current with a resulting saving in lost time.

5. The unit is inexpensive, simple, compact, easy to install and adjust. It is entirely automatic, since the response was designed for uniform compensation for any fractional heat current settings.

References

1. STRUCTURAL SPOT WELDING—CONSISTENCY CONTROL METHODS, A. M. Unger. *Welding Journal*, October 1941, pages 687–93.

2. VOLTAGE VARIATION OF TOLERANCE FOR A-C RESISTANCE WELDING, L. G. Levoy. *Welding Journal*, July 1942, pages 454–60.

3. AMPERE-SQUARED RECORDER, T. A. Rich. AIEE TRANSACTIONS, volume 60, 1941, pages 839–41.

Thyratron Motor Control

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Synopsis: The control of d-c motors was one of the first electronic-control projects. Recent developments, particularly the fully automatic and co-ordinated control of the armature power circuit and the field power circuit, have greatly increased the acceptability of these controls for industrial use.

This paper describes the control functions associated with an all-electronic d-c motor drive, in which grid-controlled thyratron-tube rectifiers automatically perform the variable voltage functions associated with the d-c generator, exciter, and field rheostats of a conventional motor-generator drive.

In addition, the grid-control action of the thyratron-tube rectifier dispenses with the usual magnetic starting and accelerating control devices and gives superior motor performance in the matter of almost idealized constant-current acceleration and flat preset speed-control characteristics resulting from *IR*-drop compensation and regulated field excitation.

Reversing is accomplished by magnetic reversing switches which reverse the armature terminals relative to the rectifier. Contact duty during switching is minimized by the supplementary action of the thyratron tubes which themselves serve to interrupt and initiate the armature current. Proper grid-control sequence in these same tubes causes them to act as inverters during the decelerating portion of the reversing cycle so that the rotational energy of the armature is actually pumped back into the a-c system.

Stopping is normally by means of a dynamic braking resistor, although the motor may be decelerated to a stop by utilizing the inverter action associated with reversing to regenerate power back into the a-c system.

Similar control schemes are applicable to motor-generator-set drives to control the generator and the motor fields in response to co-ordinated signals of motor speed and loop currents.

THE basic problem of realizing the flexibility of a d-c motor where only a-c power is available can be readily solved by the use of thyratron-tube rectifiers. The source of direct current is obtained from the rectifier, and the grid-control feature offers a means of controlling the motor by low-energy grid circuits.

The control can make the motor perform in practically any way desired. The speed can be controlled in accordance with requirements of the machine it drives, such as holding tension in a wire-reeling machine or holding loops between the stands of a rubber, cloth, paper, or steel mill. The speed can be held constant over a wide range of load or line-voltage conditions. The torque output can be held substantially constant from zero to base speed. Protection during acceleration and on overload is possible by means of current limit built into the control.

The rating of the rectifier, the type of circuit used, and the rating of the individual power tubes determine the size of motor which can be so controlled. In the case of larger size motors, the output wave shape is more of a determining factor on the circuit used than the size of the motor. The rectifier circuits used conform to standard rectifier practice and will not be covered in detail in this paper. The conventional biphase half-wave circuit controlling a one-horsepower 230-volt d-c motor will be used as the example in describing the theory of operation. It will be noted that two rectifiers are required, one for the armature power and one for the field. When the motor is to be operated from base speed down, by varying the armature voltage, the field rectifier is uncontrolled. A variable-speed motor can be made to cover a wide speed range by using a controlled rectifier on the field as well as on the armature.

The armature rectifier consists of grid-controlled thyratrons, and the output voltage is varied by shifting the phase of the grid voltage of the thyratrons with respect to their anode voltage. There are several ways of shifting the grid voltage, but the one used in these controls is the conventional inductance–resistance bridge. The inductance will be the variable element by the use of a saturable reactor. In this way the output of the rectifier can be controlled by the small d-c current in the saturating winding of the saturable reactor. Such a circuit is shown in Figure 1.

Auxiliary D-C Control Power and Voltage Standards

Since direct current is required for these saturating windings, a source is provided by a small thermionic rectifier and anode supply as shown in Figure 2. The d-c voltage is filtered by means of a reactor $X1$ and capacitor $C1$ and fed through a current-limiting resistor $R1$ and the voltage-regulating tubes A and B.

Tube B is primarily used as a constant voltage reference against which various signal voltages are compared. It is an inherent characteristic of a glow tube that its terminal voltage will remain essentially constant at rated value even though the current through the tube changes widely. The tubes used in this circuit maintain approximately 75 volts, and it is by this means that a reference voltage for regulating purposes is established that will remain relatively constant irrespective of a-c line-voltage changes.

Tube A is connected in series with tube B to give a constant potential three-wire d-c system of 150/75 volts for the plate and grid voltages of the control tubes.

The d-c winding of a saturable reactor is connected in series with a triode vacuum tube C, and this combination is connected across the 75-volt d-c control bus furnished by regulator tube A. (See Figure 3A.) With a negative voltage on the grid of tube C there will not be any plate current, the reactor will be unsaturated, and the thyratrons are phased off. If the grid is made less negative, tube C will pass some current, and the reactor will be partially saturated, which will advance the phase of the grid voltage. As the grid voltage of tube C approaches zero, the reactor will be completely saturated, and the thyratrons will be full on.

Armature-Voltage Control

The control scheme consists of comparing a portion of the armature voltage with a preselected portion of the voltage-regulator tube's voltage. The difference between these two voltages is amplified and applied to the grid of the triode, which saturates the reactor controlling the grid phase shift on the thyratrons supplying the armature of the motor.

To couple the armature voltage to the triode which saturates the reactor, it is necessary to have a common point for the signal voltage from the armature and for the grid voltage applied to the triode. It is also necessary that the relation of these voltages be correct; that is, an increase in armature voltage above a preset voltage level should make the grid of tube C more negative so as to retard the phase of the thyratron grid voltage. By studying Figure 3B, it will be apparent

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that the voltage relations are incorrect and, if directly connected, would give incorrect operation. It is therefore necessary to change this relation.

This can be done as shown in Figure 3C as follows: The grid of tube *C* is connected to a voltage divider across the 150-volt control bus so that the grid is positive when tube *D* is passing very little or no current. Then, as more current is caused to flow through tube *D*, the grid of tube *C* will be pulled negative by the increased current through *R*2. It is now possible to control the phase of the thyratron grid voltage by changing the voltage on the grid of tube *D* with the relation correct for regulation of the armature voltage.

Armature-Speed-Control Potentiometer

With the cathode of tube *D* connected to the mid-point of a voltage divider across glow tube *B*, as shown in Figure 3C, the cathode will be $37^1/_2$ volts positive with respect to the negative bus. With zero voltage on the armature, the grid of tube *D* will be $37^1/_2$ volts negative, and the thyratrons will be in a condition to conduct full half cycles. As voltage on the armature builds up to nearly $37^1/_2$ volts, the thyratrons will shut off just enough to maintain a balance between the armature voltage and the $37^1/_2$-volt reference.

In order to permit the adjustment of the armature voltage or speed, resistor *R*5 is replaced with a potentiometer so that the cathode potential of tube *D* can be changed. In order to permit the use of 230 volts on the armature of the motor, a voltage divider is connected across the armature and a portion of the armature voltage compared to the reference voltage. This is shown in Figure 4. The potentiometer shown in the armature voltage divider gives a means of setting the base speed with the speed control set for maximum speed. This adjustment gives a means of compensating for commercial tolerances in resistors and tubes.

IR-Drop Compensation

Armature speed is proportional to armature terminal voltage only when the armature is drawing no current and is spinning freely with the field excited. Under these conditions, the armature voltage is the counter electromotive force or generated voltage of the armature conductors. When current flows in the armature, as when the motor shaft is loaded, the counter electromotive force and hence the true speed is less than the terminal voltage by an amount equal to the voltage drop in the resistance of the armature conductors themselves. If a constant shaft speed is to be maintained irrespective of load, it is necessary to raise the terminal voltage progressively as the load current increases, by an amount equal to the armature-resistance voltage drop. To do this it is only necessary to introduce in the armature-control circuit a voltage proportional to the armature current in such a direction as to hold a higher armature voltage as the load on the motor increases.

Although it would be possible to build an amplifier system that would amplify the millivolt drop of a conventional shunt to such a point that it could be used, it offers serious difficulties. In order to get a more suitable voltage, a special current transformer is designed to operate in the anode leads of the rectifier tubes. The pulses of current through these anode leads are fed into the two primary windings so as to act as an alternating current that bears a direct relation to the direct current. The secondary winding of this current transformer is loaded by a resistor and a full-wave rectifier, and the resultant d-c voltage is directly proportional to the armature current. The value of this voltage for a given current can be adjusted with a potentiometer.

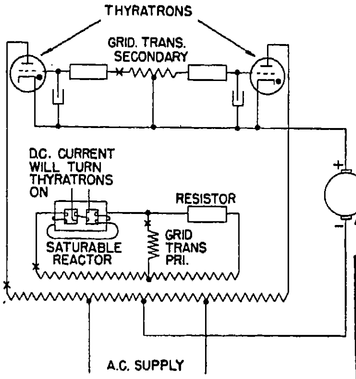


Figure 1. Grid-controlled rectifier with phase-shift bridge

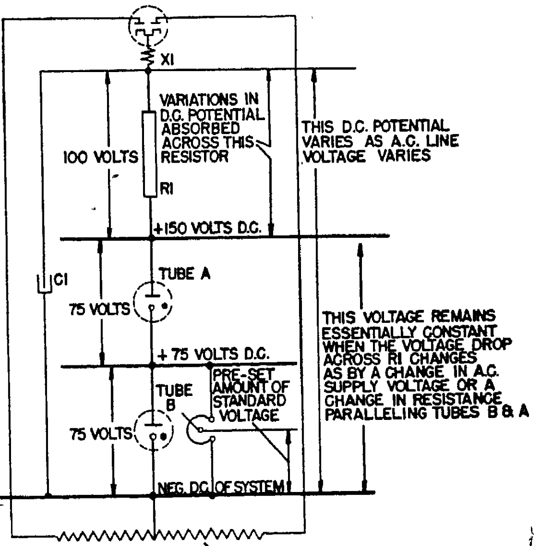

X1
VARIATIONS IN D.C. POTENTIAL ABSORBED ACROSS THIS RESISTOR
100 VOLTS
R1
THIS D.C. POTENTIAL VARIES AS A.C. LINE VOLTAGE VARIES
+150 VOLTS D.C.
C1
TUBE A
75 VOLTS
+ 75 VOLTS D.C.
THIS VOLTAGE REMAINS ESSENTIALLY CONSTANT WHEN THE VOLTAGE DROP ACROSS R1 CHANGES AS BY A CHANGE IN A.C. SUPPLY VOLTAGE OR A CHANGE IN RESISTANCE PARALLELING TUBES B & A
TUBE B
PRE-SET AMOUNT OF STANDARD VOLTAGE
75 VOLTS
NEG. DC OF SYSTEM
SEPARATE A.C. ANODE VOLTAGE

Figure 2. Regulated rectifier for control amplifiers

This d-c voltage that is proportional to armature current is connected in the lower end of the armature-voltage-divider circuit as shown in Figure 5. This added voltage in effect subtracts from that part of the armature voltage used as a feedback and therefore causes a higher voltage to be maintained by the regulating action. The adjustment provided permits setting the control for only partial compensation, or it can be overcompounded.

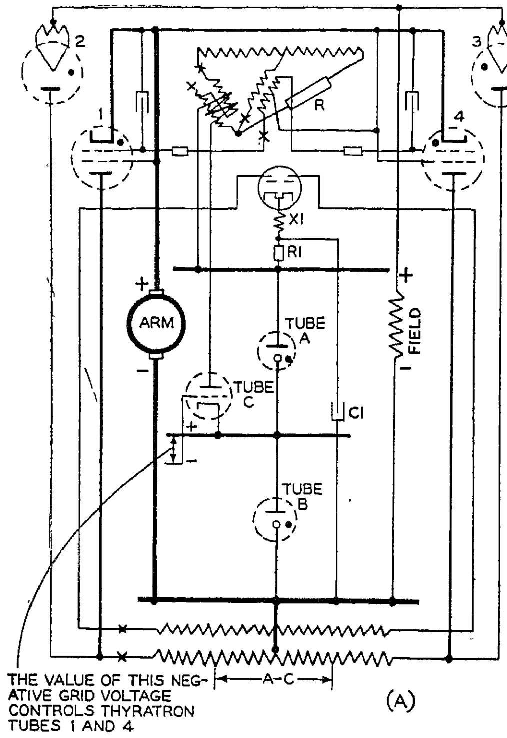
Figure 5A shows the effectiveness of the *IR*-drop compensation. The dotted curves represent the normal uncompensated motor-regulation characteristics. The solid curves were taken with the *IR*-drop compensation set for flat compounding at 100 per cent rated speed. Note the great improvement in the motor characteristics at the lower speeds.

Current Limit

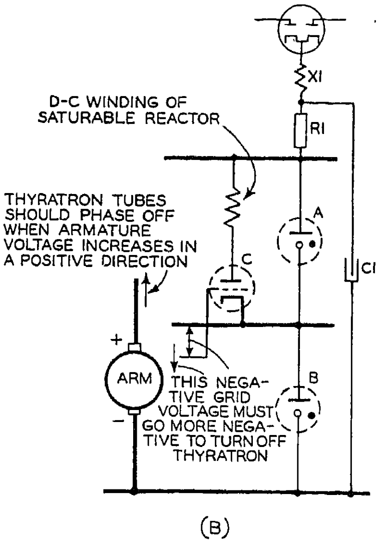
Current limit is necessary to prevent excessive currents being drawn during acceleration so as to protect the tubes and keep within the limits of commutation of the motor; otherwise, overloads could occur by suddenly applied shaft loads or by starting with a preset speed adjustment. A current-limit control should be inoperative from zero current up to the preselected value and then prevent the current rising above this value. If this were not the case, the current would be influencing the speed; instead, it should take over sharply as near the preset current value as possible.

A d-c voltage proportional to armature current has been established for *IR*-drop compensation, and this same voltage can be used for current limit. A current-limit signal is established by comparing a portion of the d-c voltage that is proportional to current with a known voltage standard and amplifying the difference by a triode. The circuit showing the connections for this control added to the armature control and *IR*-drop compensation is shown in Figure 6.

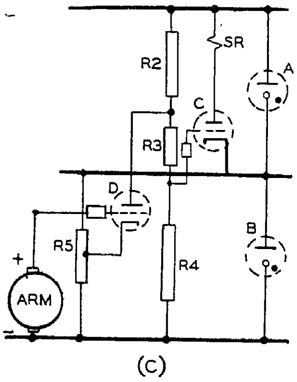
The value at which the current limit takes over is adjustable by means of the current-limit potentiometer. As long as the voltage on the slider is less than 75 volts, the current-limit tube *E* will be biased to cutoff, or practically so, and the armature-control tube *D* will have complete control. If high armature current causes the voltage on the current-limit-potentiometer slider to reach 75 volts, tube *E* will pass full anode current which will retard the grid voltage phase on the thyratrons. This reduces the current, and a balance will be reached. When the load on the motor is reduced and the armature current goes below the preset value, the armature-control tube *D* will take over and hold armature voltage as long as the current stays below the current-limit point.


2
3
1
4
R
X1
R1
+
ARM
−
TUBE A
TUBE C
C1
+
FIELD
−
TUBE B
A-C
THE VALUE OF THIS NEGATIVE GRID VOLTAGE CONTROLS THYRATRON TUBES 1 AND 4
(A)

A (left). Variable d-c source for saturable reactor


X1
D-C WINDING OF SATURABLE REACTOR
R1
THYRATRON TUBES SHOULD PHASE OFF WHEN ARMATURE VOLTAGE INCREASES IN A POSITIVE DIRECTION
A
C
C1
+
ARM
−
THIS NEGATIVE GRID VOLTAGE MUST GO MORE NEGATIVE TO TURN OFF THYRATRON
B
(B)

B (above). Showing that armature voltage has incorrect sense to be coupled directly to grid of tube *C*



C (left). Showing that armature voltage now has correct sense to be coupled directly to tube *D*

If armature voltage tends to increase (becomes more positive), the grid of tube *D* becomes less negative and tube *D* turns on which turns tube *C* off which reduces saturation of *SR* which retards grid phase and reduces thyratron output

Figure 3. Armature-voltage-regulation circuit

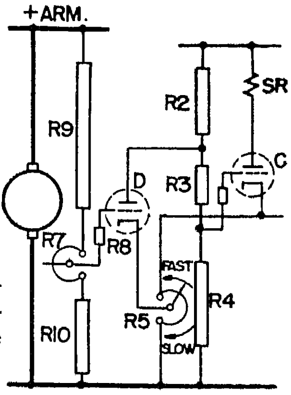
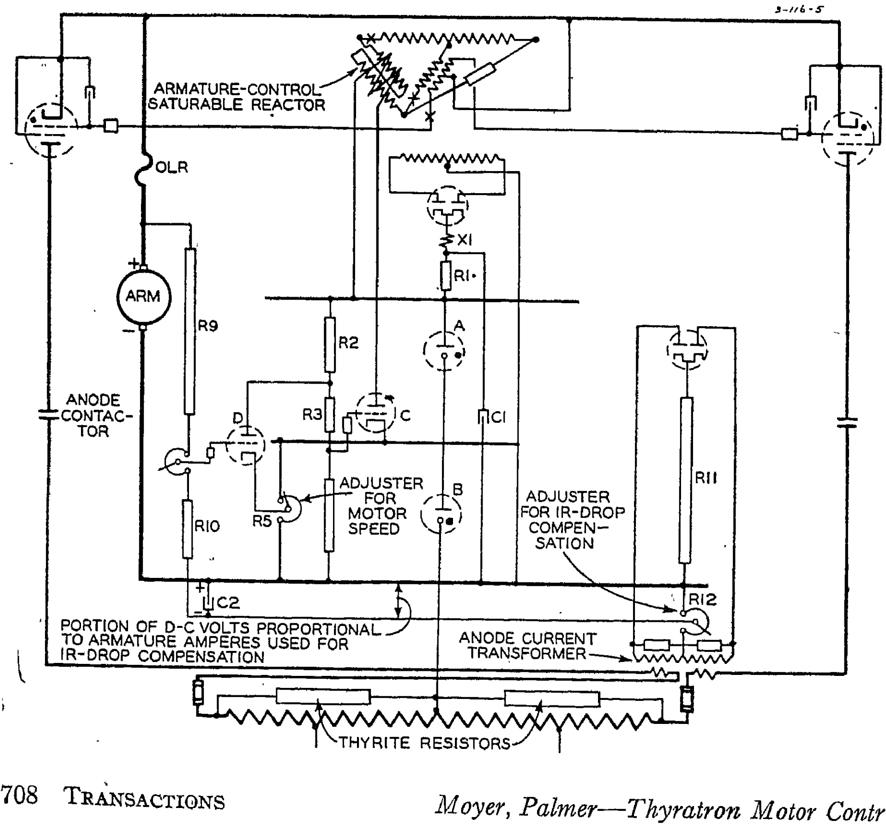


Figure 4. Armature-voltage-regulating circuit complete with armature-voltage divider

Figure 5 (below). Armature-voltage-regulation circuit with *IR*-drop compensation added


3-116-5
ARMATURE-CONTROL SATURABLE REACTOR
OLR
X1
R1
+
ARM
−
R9
R2
A
ANODE CONTACTOR
D
R3
C
C1
R11
ADJUSTER FOR MOTOR SPEED
B
ADJUSTER FOR IR-DROP COMPENSATION
R5
R10
+
C2
R12
PORTION OF D-C VOLTS PROPORTIONAL TO ARMATURE AMPERES USED FOR IR-DROP COMPENSATION
ANODE CURRENT TRANSFORMER
THYRITE RESISTORS

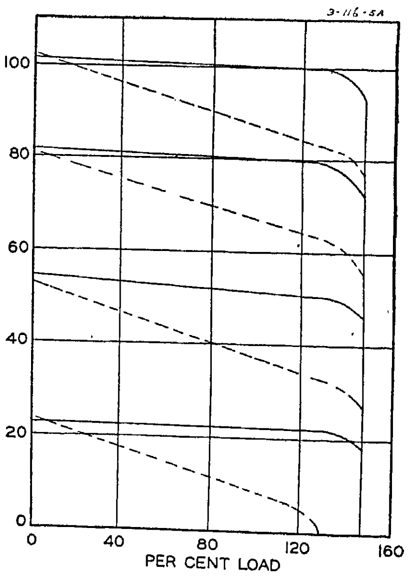


Figure 5A. Speed-regulation curves showing effect of *IR*-drop compensation

Dotted regulation curves are without *IR*-drop compensation. Solid regulation curves are with *IR*-drop compensation

With this type of control it is possible to accelerate from zero to full speed without drawing more than the preset value of current through the armature. During the operation of the motor, if excess load is applied, the speed will be maintained up to the point where the current reaches the current-limit setting. Beyond this point the preset value of current will be maintained in the armature even down to the stalled armature condition. The current will be maintained until the thermal overload relay functions.

When starting from rest with zero current or when reversing, there is no current to generate a current-limit voltage, and the armature voltage is zero; therefore, the first few half cycles of current might be of destructive magnitude. To overcome this objection, a normally closed interlock on the initiating contactor is used to give a false signal in the current-limit circuit. By means of a capacitor this false signal is "remembered" for a time after the circuit is closed and makes the current build up to the current-limit value rather than have one large surge of current and come down to the correct value.

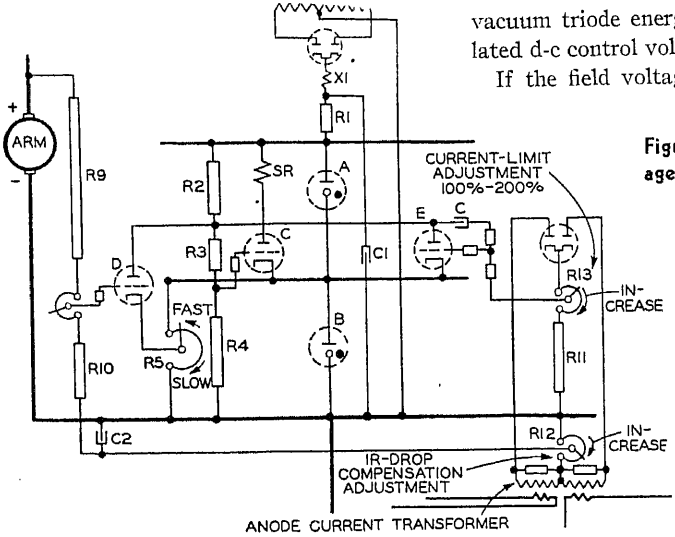

ARM
+
-
R9
R10
C2
R2
R3
R4
R5
FAST
SLOW
D
SR
C
A
B
X1
R1
C1
E
C
CURRENT-LIMIT ADJUSTMENT 100%-200%
R13
IN-CREASE
R11
R12
IN-CREASE
IR-DROP COMPENSATION ADJUSTMENT
ANODE CURRENT TRANSFORMER

Figure 6. Armature-voltage-regulation circuit with current limit added

Stopping

The rectifier characteristics of the thyratrons supplying direct current to the motor prevent the reversal of power from the motor to the a-c system when the motor is running faster than the speed called for by the control. This condition exists, for example, when the speed-control potentiometer is suddenly turned to a lower speed. In the case of high inertia loads this "electrical free-wheeling" characteristic is objectionable, but on friction loads it is of no consequence.

When power is removed for stopping, the same system of dynamic braking can be used as is found on magnetic control; that is, a resistor may be connected across the armature to absorb the stored energy. If quick slowdown is required, it is possible to add a control tube and a relay that will actuate the dynamic braking contactor to connect a resistor across the armature when the motor speed is above that called for by the control. When the speed is reduced to the called-for value, the tube initiates the function of disconnecting the resistor.

Field-Weakening Control

In order to expand the range of speed control, the shunt field rectifier tubes can be replaced with thyratrons and control added to the field. The control of the field rectifier is accomplished by using another saturable reactor and resistor bridge with the d-c winding of the saturable reactor saturated through a high vacuum triode energized from the regulated d-c control voltage.

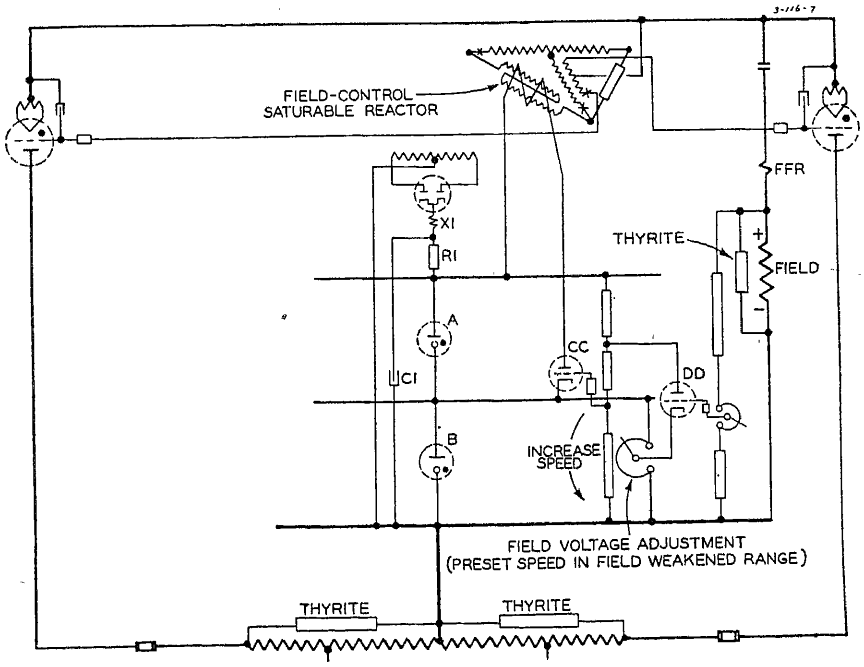
If the field voltage is accepted as an indication of field flux, the control system can be similar to the armature-control circuit in that a voltage-regulating system is used. This regulating circuit is shown in Figure 7. The armature controls are not shown, to avoid confusion. The electric feedback of the field-voltage signal will maintain a preset level of field excitation independent of line-voltage variations.

Where the motor speed is being controlled by varying the armature voltage, the field excitation should remain at rated value to develop rated torque. Conversely, when operating the motor in the weak field range, the armature voltage should remain at rated value to develop constant horsepower. This characteristic can be had by ganging both the armature-control potentiometer and the field-control potentiometer on the same shaft. To do this each control is compressed into half the rotation, with the other half a conducting segment of negligible resistance. These are staggered so that with the armature-control slider in its active range the field-control slider will be on the conducting segment holding full field.

Speed Regulation at Reduced Field Excitation

When the field excitation is reduced the ratio between speed and armature counterelectromotive force is changed such that the armature must rotate at a higher speed in a weaker field in order to generate a given electromotive force. A given preset speed level is maintained relatively constant by grid phase control of the armature thyratrons as they phase

Figure 7 (below). Field rectifier with voltage control


FIELD-CONTROL SATURABLE REACTOR
FFR
THYRITE
+
FIELD
-
X1
R1
A
C1
CC
DD
B
INCREASE SPEED
FIELD VOLTAGE ADJUSTMENT (PRESET SPEED IN FIELD WEAKENED RANGE)
THYRITE
THYRITE

advance to compensate for *IR* drop accompanying changes in armature current.

In this system speed is not regulated by changes in field excitation. Speed is only preset to new levels by the field control but is regulated by armature control. When the armature voltage has been increased to the limit determined by the anode voltage, there can be no further regulatory action.

Field Excitation During Acceleration

It is necessary to apply full excitation to the motor field in order to accelerate it from standstill to base speed. Above base speed the field should be weakened slowly enough to keep the armature current within the commutating ability of the motor. Therefore, the application of armature voltage and the weakening of the field must be controlled by the current-limit circuit. By doing this, the motor is made to accelerate at the maximum possible rate permissible without drawing excessive current. Full field during acceleration from zero to base speed is maintained regardless of the setting of the field-voltage control.

The circuit that accomplishes the aforementioned features is shown in Figure 8. It is similar to the current-limit control on the armature voltage shown in Figure 6 in that the d-c voltage proportional to armature current is compared with a standard voltage, and the difference voltage is applied to a triode. The triode acts on the saturating circuit of the grid phase shifting reactor of the field rectifier.

There are two differences between the armature and the field control. First, an increase in armature current beyond a predetermined amount must increase the field strength by saturating the saturable reactor. When controlling the armature voltage, an increase in armature current necessitates a decrease in the saturation of the armature-control saturable reactor. Second, the standard voltage is not directly the constant voltage of regulator tube *B* but is the slightly variable voltage between the grid of tube *CC* and the negative bus, which voltage is in turn referred to the standard voltage of tube *B*.

The field forcing triode tube *EE* is connected so that, when it is caused to conduct by high armature current, the grid of tube *CC* will be pulled positive. The increased current in the anode of tube *CC* will increase the saturation of the field-control saturable reactor and apply more field.

Figure 8. Voltage-regulated field control with armature-current limit added

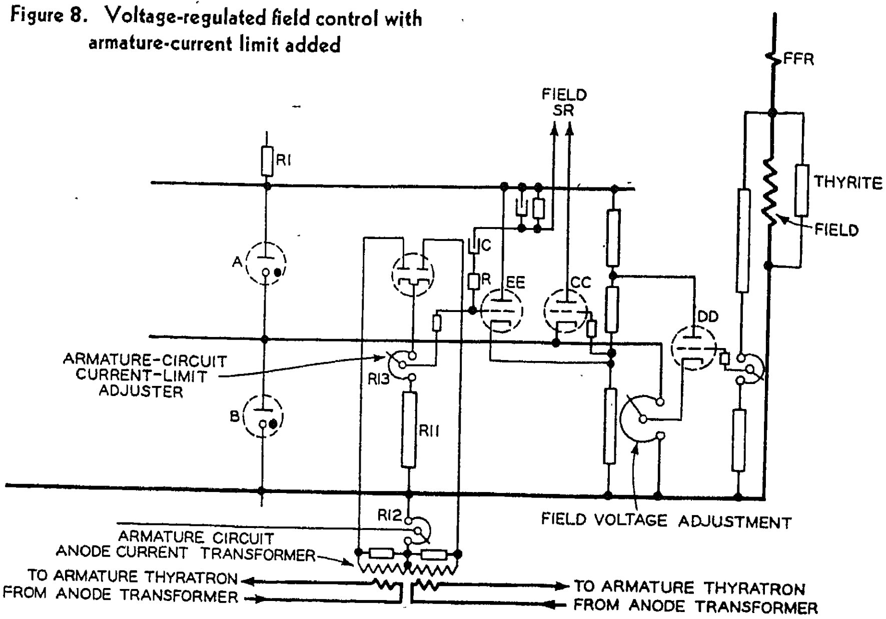

FFR
FIELD SR
R1
THYRITE
FIELD
A
C
R
EE
CC
DD
ARMATURE-CIRCUIT CURRENT-LIMIT ADJUSTER
R13
B
R11
R12
FIELD VOLTAGE ADJUSTMENT
ARMATURE CIRCUIT ANODE CURRENT TRANSFORMER
TO ARMATURE THYRATRON FROM ANODE TRANSFORMER
TO ARMATURE THYRATRON FROM ANODE TRANSFORMER

Figure 9. Armature-current-limit control connected to both the armature-voltage and field-voltage control

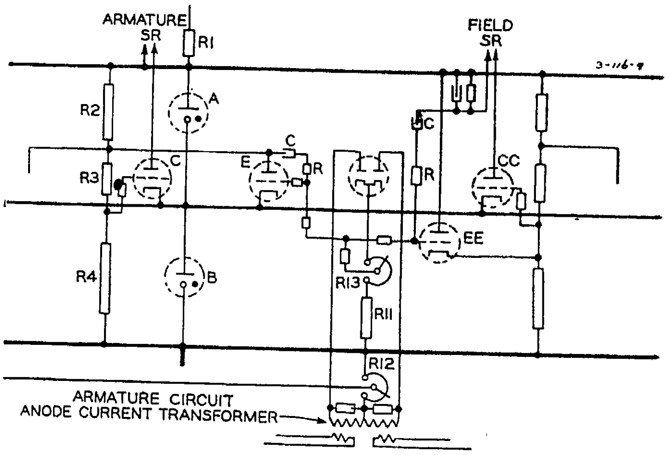
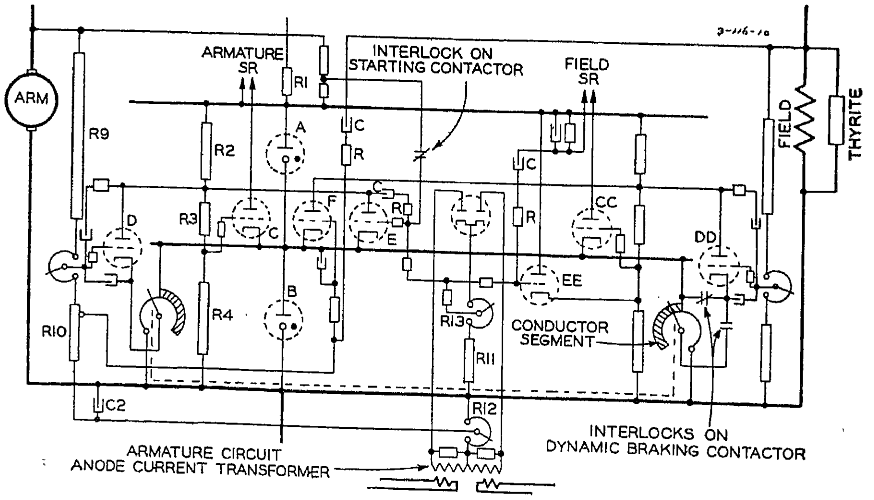


Figure 10 (below) Complete armature- and field-voltage controls


3-116-10
ARM
ARMATURE SR
R1
INTERLOCK ON STARTING CONTACTOR
FIELD SR
FIELD
THYRITE
R9
A
C
R2
R
C
D
R3
C
F
R
E
R
CC
DD
EE
R4
B
R10
R13
CONDUCTOR SEGMENT
R11
C2
R12
INTERLOCKS ON DYNAMIC BRAKING CONTACTOR
ARMATURE CIRCUIT ANODE CURRENT TRANSFORMER

The normal operation of the circuit shown in Figure 8 will put the grid of tube *CC* and the cathode of tube *EE* slightly negative of the neutral bus, or almost 75 volts positive with respect to the negative bus. The grid of tube *EE* is connected to the negative bus except for the voltage proportional to armature current. This condition means that field forcing tube *EE* will be nonconducting until the armature current reaches a preset value determined by the setting of *R*13. Therefore, with the armature current below this preset value, the field regulator will operate normally, but tube *EE* will take over and increase the field as soon as the armature current exceeds the preset value.

The field forcing triode tube *EE* will act ahead of the armature current-limit

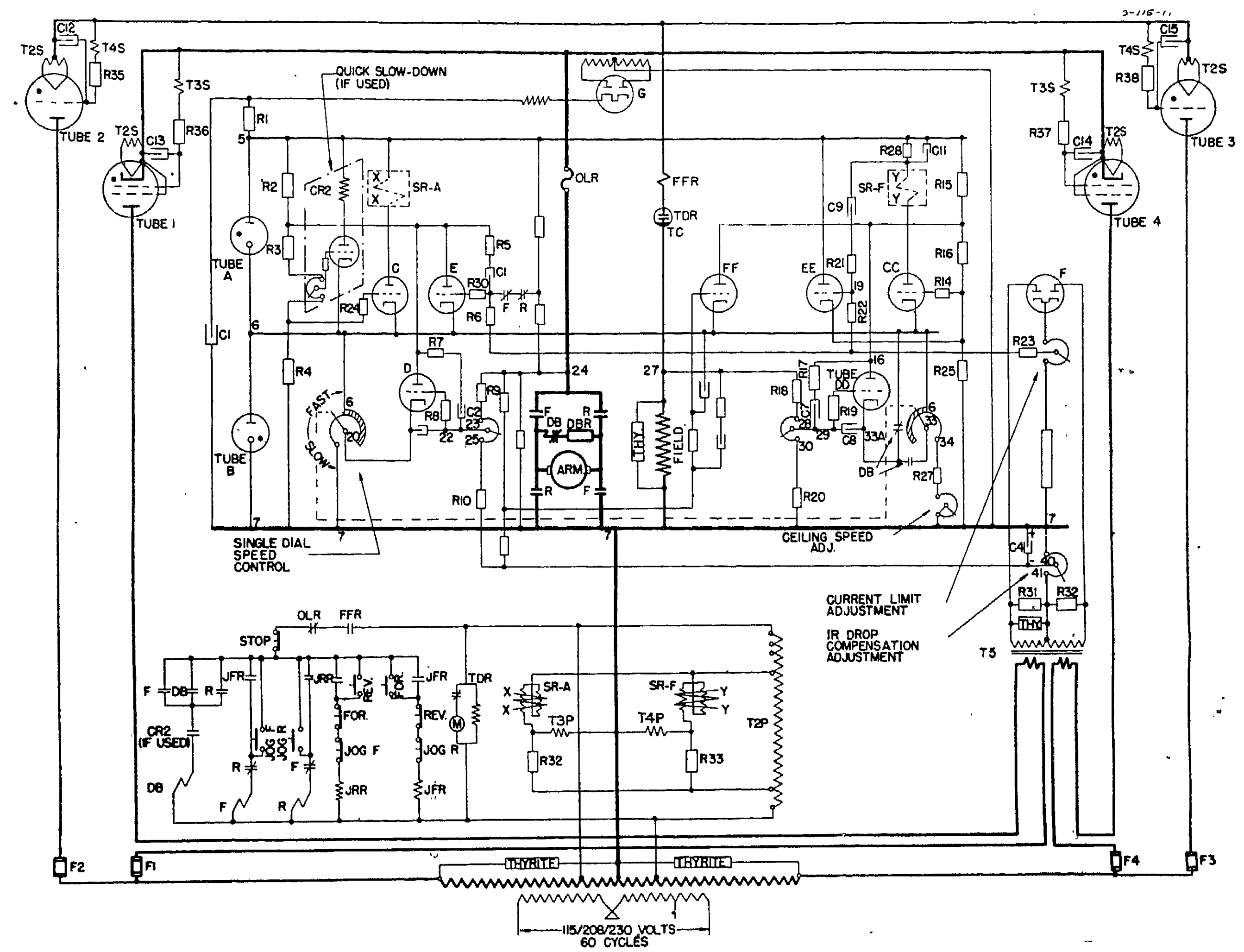


Figure 11. Complete power and control circuits of an electronic drive for a one-horsepower 230-volt d-c shunt motor

triode because the reference voltage of tube *EE* is slightly less than tube *E*. This is diagrammed in Figure 9 where both the field forcing and armature voltage control are shown connected to the same current-limit signal voltage. In the case of suddenly applied loads that do not give time for the field flux to build up to the needed value, the armature voltage will be reduced and prevent overloading the armature supply tubes. The two controls work independently and are arranged so that the field control works at a slightly lower value than the armature control.

The field circuit may be preconditioned to full field value prior to starting by means of interlocks on, say, the dynamic braking contactor. Then, when the starting is initiated, the false signal is removed, but its effect dies off more gradually because of capacitor discharge circuits and maintains full field until the armature current can take control via tube *EE*.

Armature-Voltage Limit When Decelerating

If the speed-control potentiometers are suddenly turned from the weak field condition to a speed calling for full field, the armature voltage will rise above the applied voltage. The voltage will rise because the full field would be applied suddenly and the motor becomes a generator. Because of the rectifying action of the thyratrons, current cannot flow from the machine into the line and dynamically brake the motor as it would be if connected directly to a d-c generator. This would be an objectionable condition, as it would endanger the insulation of the commutator and associated equipment.

This high generated voltage can be avoided by taking a signal from the armature voltage and actuating a triode that will retard the application of field when the voltage exceeds a preset amount. This preset value can be well above the operating value so that it will not interfere with normal control. This control is accomplished by tube *F* of Figure 10.

On nonreversing controls where the armature circuit is interrupted by an anode contactor instead of contacts at the armature terminals this armature-voltage-limit feature allows the use of a lower value of dynamic braking resistor than would normally be used on a motor being stopped from a weak field condition. The delayed field strengthening prevents excessively high armature currents when dynamic braking to rest by limiting the voltage across the armature.

Reversing Control

When the armature circuit is reversed by magnetic contactors between the output of the thyratrons and armature terminals, the duty on the contacts is much less than when the motor is supplied from a d-c generator. The duty on the contacts is reduced because the action of the thyratrons assists in interrupting the armature current. Because of the combination of inverter action and armtuure-current-limit control, the reversing cycle is such as to cause a uniform and rapid deceleration to zero speed, followed by acceleration to the preset speed in the reverse direction. During deceleration to zero speed, the rotational energy of the armature and the load is pumped back into the a-c system with the armature thyratrons. The preset speed in

The Effects of Mutual Induction Between Parallel Transmission Lines on Current Flow to Ground Faults

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THIS paper is intended to present some of the problems encountered in determining unbalanced fault currents and unusual results that may be expected on systems with parallel lines on a common right of way. Correct results are required so that proper co-ordination and application of relays may be accomplished. Fault studies must be made and constantly revised, as a power system changes and expansions take place. Also, future conditions must be studied so that intelligent selection may be made of circuit breakers and current transformers. It is the purpose of this paper to discuss

1. The use of the a-c network analyzer in solving fault problems by using certain mathematical simplifications which are necessary in order to adapt the problem to the analyzer's limitations.

2. Some rather unusual conditions which may occur on a system during fault conditions because of the effect of mutual induction between parallel lines.

Frequently two or more power transmission lines are built on the same right of way parallel to each other for partial distances or for the full length. The problem of mutual induction between such circuits arises when unbalanced faults occur on one line causing induced voltages in the parallel circuit. Where the lines are of the same voltage and bussed at either or both ends, the calculation resolves itself into the use of a simple equivalent circuit. The problem becomes more complicated when the lines are operated at different voltages which obviously cannot be bussed at one end, or when a fault calculation is required on one end of a pair of parallel lines whose opposite ends are unbussed, such as a split-bus arrangement. In solving a problem of the latter type, use is made of the a-c network analyzer which utilizes one-to-one ratio transformers to represent the mutual induction between lines.

Usually fault-current calculations are within the scope of a d-c calculating board. As a power system becomes more complicated and more than one line occupies the same right of way, in some cases operating at different voltages, the effect of mutual induction between the lines becomes important for unbalanced faults. A d-c board cannot be used for these more complicated conditions.

Zero-Sequence Currents

Since the flow of zero-sequence current is not restricted to the physical conductor but must return through the ground circuit, the resistivity of the ground itself enters into the determination of its impedance. The effect of the ground return circuit upon the impedance can be determined by either one of two ways; namely, by direct measurement after the line is constructed, or by assuming an average value of ground resistivity for calculating the impedance by formula. The latter method is the one which must be used in most cases (see appendix). It is not deemed within the scope of this paper to discuss the various theories of current flow through the ground but to show how assumed conditions are used to obtain what are considered reasonable results.

The effect of mutual coupling or the resultant induction between parallel circuits is inversely proportional to the spacing between the circuits and directly proportional to the length of parallel.

Mutual Induction

When two transmission lines parallel each other close enough for the mutual induction to be appreciable, there is transformer action between the circuits when unbalanced currents flow. The mutual induction resulting from the flow of positive-sequence current is small and is reduced to a negligible value by the transposition of conductors. Transposition, however, has no effect on the zero-sequence induction. When paralleling two identical circuits of the positive-sequence network, the resulting impedance is one half of the impedance of one line. This is not true for the zero-sequence network for the reason that the flow of unbalanced currents will cause a certain amount of induction or mutual coupling to exist between the circuits resulting in a value somewhat greater than half the impedance for two identical circuits in parallel. The mutual impedance between the circuits tends to reduce the fault current under certain conditions and increase it under other conditions, de-

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each direction can be independently controlled by connecting different control potentiometers with interlocks on the reversing contactors.

Application to Motor-Generator-Set Drives

For those who have immediate need for this type of control characteristic for motors of, say 50- to 400-horsepower ratings, or even 2,000-horsepower, for that matter, it is practical to modify this equipment so that the thyratron functions associated with variable armature excitation of a one-horsepower motor would, instead, provide field excitation for the generator of a motor generator set, and the thyratron circuit for the field of the one-horsepower motor would be adapted to supply variable field excitation to the field of the larger motor. The loop-current control becomes more complex, particularly since d-c current must be measured directly and its direction of flow ascertained so as to give correct sequence of excitation to the respective fields of generator and motor during acceleration and deceleration. The reversing problem, too, is more complicated because of the necessity of reversing generator field excitation, but all this has been done.

Meanwhile, developments are progressing toward the use of ignitron-type tubes to provide armature excitation for motors of higher horsepower ratings than can now be supplied with the available hot-cathode thyratron tubes.

pending upon the relative directions of the ground current and the induced current. When a single-phase-to-ground fault occurs on an energized line, the resulting zero-sequence currents flowing through all three conductors in the same direction induce a voltage in the coupled circuit tending to cause an induced current to flow in the opposite direction to the one causing it. This may add to or subtract from the existing zero-sequence currents flowing in the coupled circuit to the fault through physical circuit ties.

Under certain conditions the amount of induction may be great enough to reverse the current flow in the coupled circuit when large fault currents flow. It may even change the direction of flow in nearby transformer neutrals, causing the current to flow in reverse of the conventional direction.

Devices for Representing Effect of Mutual Induction

Two lines having mutual induction between them may be represented by an equivalent circuit, provided the lines are bussed at one end. This presupposes that they are operated at the same voltage. The equivalent impedance is determined by subtracting the mutual impedance from the zero-sequence impedance of each line and adding it as a common impedance permitting solution by a reduction of circuits through arithmetical means. See Figure 1A.

This equivalent circuit may be used on an analyzer, but the solution is limited in its scope to lines of the same voltage bussed at one end. A device providing a more general and flexible arrangement is used on the network analyzer with alternating current applied to the analyzer. A transformer with one-to-one ratio is used, shunting an impedance equal to the mutual impedance across one set of terminals such that a voltage drop proportional to the mutual induction is reflected to the coupled circuit, inducing a current in the opposite direction to the originating current. This device may be used for lines of differing voltages and for unbussed lines. The analyzer setup is the same as for the equivalent circuit, and therefore requires that the mutual impedance be subtracted from the zero-sequence impedance of each line and that one side of the transformer be shunted with an impedance equal to the mutual impedance. Either side of the transformer may be shunted by the mutual impedance.

Modern systems may have some rather complicated conditions involving mutual impedance resulting from the construction of several lines on the same right of way, and their operation at different voltages. In solving problems of this type, resort must be made to simplification of circuits because the usual a-c network analyzer has few transformers for representing mutual induction. Where a number of mutual reactances are to be considered, as many simplifications as possible are made to represent the impedances between busses, and expansions are made successively by sections throughout the system as a detailed study is made.

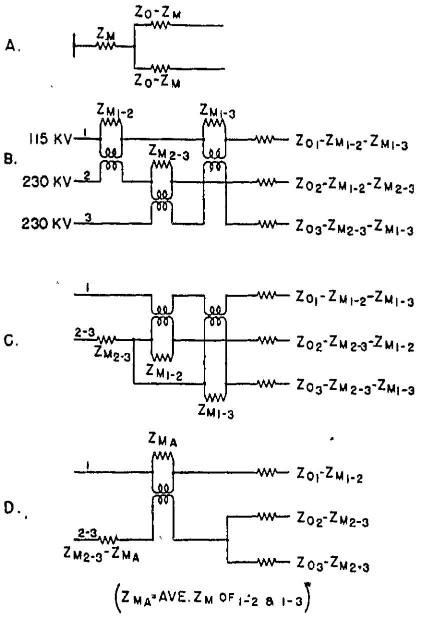

Figure 1A. Equivalent zero-sequence impedance circuit of two parallel lines (same voltage) for calculation of ground-fault current

Figure 1B. Use of one-to-one ratio transformers for representing the mutual induction between parallel lines on the a-c calculating board

Figure 1C. Simplification of the circuits in Figure 1B to reduce the number of one-to-one transformers required for the setup on the calculating board

Figure 1D. Further simplification of Figure 1B. This is only an approximation and is not accurate for all applications

Figure 2 shows a section of a possible system between stations *A*, *B*, and *C* with its several parallels. Figure 3 shows how the section was reduced to an approximate equivalent circuit requiring only two instead of six mutual transformers. Negative values of resistance and reactance may be encountered. The resistance component must be omitted. The reactance component may be handled in one of several ways. Where possible, the negative values of reactance may be combined with the positive values such as would be the case with the primary circuit of the station *B* transformer bank in Figure 3, losing the identity of the station *B* 230-kv bus. Where this cannot be done, a capacitive reactance may be substituted for the negative value of inductive reactance on an a-c analyzer. The diagram in Figure 3 could be further reduced to save reactor units on the analyzer by using a delta-star transformation of the lines, but the net result still shows a negative value of reactance. Negative reactance values can sometimes be avoided if the mutual transformers have a ratio equal to the line voltage ratio. This simplified network would be

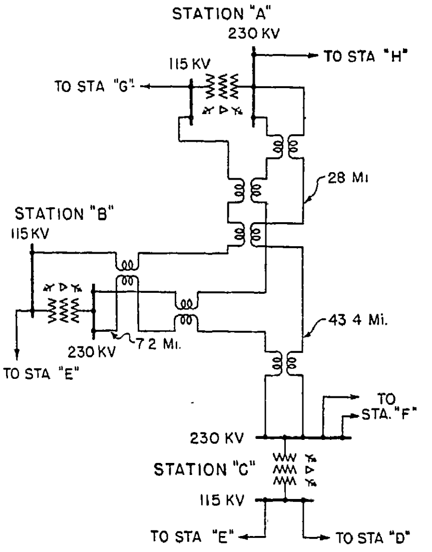

Figure 2. Diagram for indicating the relationships of mutual induction between the parallel lines connecting stations *A*, *B*, and *C*.

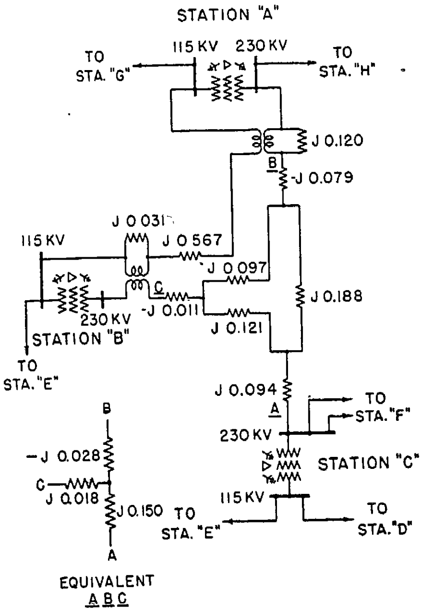

Figure 3. Simplification of the calculating-board circuits representing the lines connecting stations *A*, *B*, and *C* for calculating faults external to this section of the power system

Reactance values are on a per unit 50-megavolt-ampere base

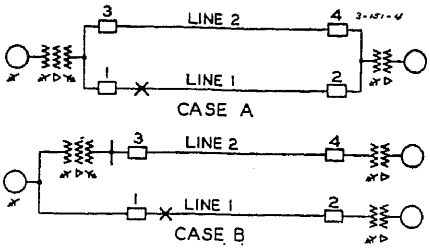

Figure 4. Examples of possible false relay operation

Case A represents two 230-kv lines operated temporarily at 115 kv

Case B represents the same pair of lines, with line 1 operated at 115 kv and line 2 operated at 230 kv, each line energized by a separate generator. The location of the ground fault in each case is marked by an *X*

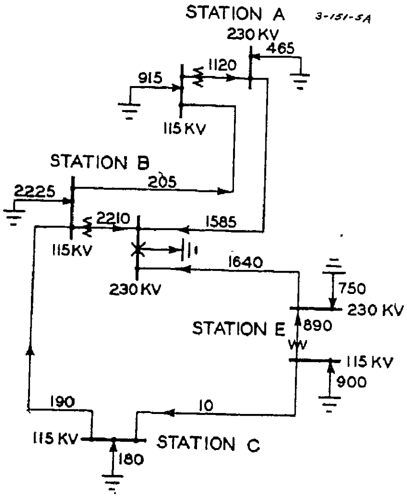

Figure 5A. Direction and magnitudes of the calculated ground-fault currents in amperes at 115 kv for a fault on the 230-kv bus of station *B* with the mutual reactances included for the calculating-board setup

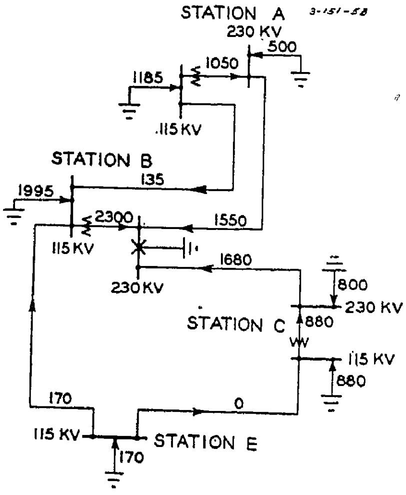

Figure 5B. Direction and magnitudes of the calculated ground-fault currents in amperes at 115 kv if the mutual reactances of the parallel lines are ignored in the calculating-board setup

Compare with current values shown in Figure 5A which include the effects of mutual induction

used during the study of faults in another section of the system and would require expansion for detailed study.

In reducing a network consisting of two lines bussed at one end and paralleled by a third line of different voltage, the arrangement is as shown in Figure 1B.

Figure 1B is reduced as shown in Figure 1C by substituting the equivalent circuit of Figure 1A for the lines that are bussed. When the values of mutual impedance are equal or close enough to average, a single transformer may be substituted as shown in Figure 1D because the sum of the induced currents flow in the mutual branch of the equivalent circuit.

False Relay Operation Because of Mutual Induction

Cases A and B, Figure 4, are two examples of how the effects of mutual induction could cause false relay operation if it is not considered in the relaying.

Case A

Case A represents two 230-kv lines operated temporarily at 115 kv. A power source is connected to each end of the circuit, and the two lines are bussed at both ends.

When the fault occurs as shown near the end of the line, a large current flows through breaker 1 resulting in quick tripping. At the same time the induction-type current-polarized directional ground relay at 2 starts to operate. After breaker 1 opens, the direction of current flow in line 2 reverses, and, because of the extra induced current from mutual induction in the line, the directional overcurrent ground relay in position 3 may seal its contacts before breaker 2 opens, causing an unnecessary trip out of the unfaulted line. Breaker 2 would probably have cleared before relay 3 closed its contacts if the added influence of mutual induction had not increased the current in line 2 to speed up the operation of relay 3.

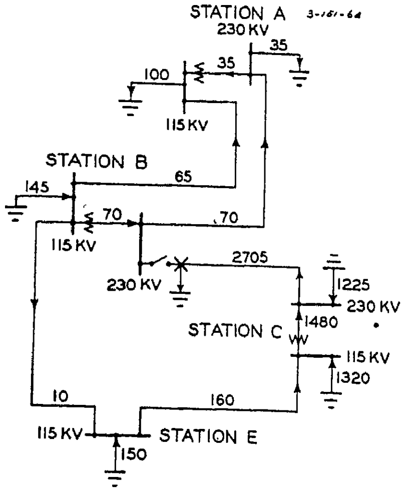

Figure 6A. Because of mutual induction between circuits it is possible to have appreciable ground currents flowing in sections isolated from the ground fault

Note current flow between stations *B* and *A*

Case B

Case B, Figure 4, represents the same pair of lines except that circuit 2 has been raised to 230 kv, and each line is energized by separate generators.

The fault is again placed at the same point on line 1, causing breaker 1 to open. This entirely isolates the two lines except for mutual induction between them which caused the unbalanced current to reverse in line 2 and therefore reverse the normal direction of the current flow in the transformer neutral. Relay 4, if current polarized, has both its polarizing and actuating current reversed so the resultant direction continues to be the same until breaker 2 opens. Relay 4 may close its contacts before breaker 2 opens, causing false relay operation of the circuit.

Effect of Mutual Induction on Ground Currents

Figures 5–7, inclusive, show diagrams with data obtained from a system fault study made on the a-c network analyzer and provide examples of the effect of mutual induction between circuits for ground faults. These examples are for the same section of the system as shown in Figure 2 except that the line from station *A* to station *C* is omitted. These

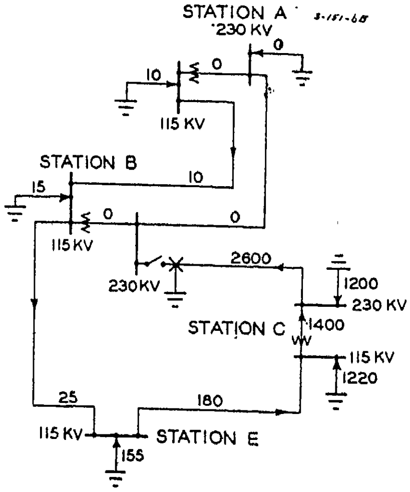

Figure 6B. The effect of ignoring mutual induction

Note negligible flow of ground currents between stations *A* and *B*

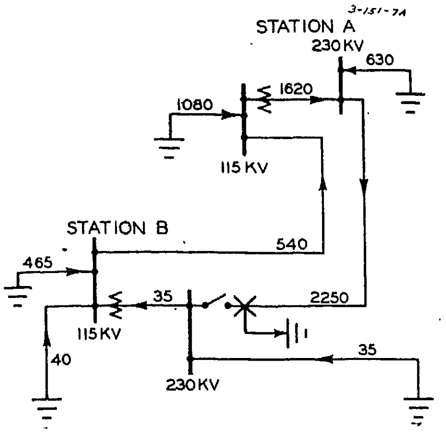

Figure 7A. The effect of mutual induction increases the over-all ground current flowing in parallel lines providing the currents flow in opposite directions

Note the effect on the station *A* to station *B* 115-kv line which parallels the 230-kv line for 35 miles. Compare with Figure 7B

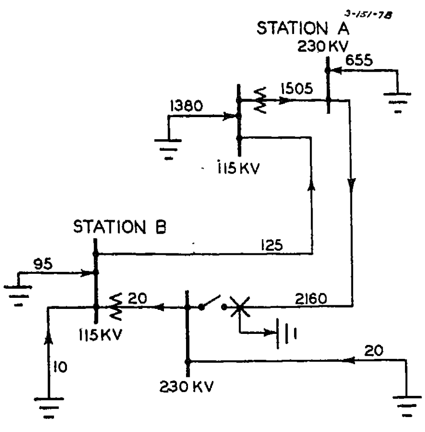

Figure 7B. Mutual induction is ignored in the calculations

The sum of the ground currents circulating in the 115-kv line and the station *B* 115-kv transformer neutral are equal to only 22 per cent of the calculated values of ground currents (shown in Figure 7A) when the mutual induction is included. In this case faulty relay operation could result if the effect of mutual induction is ignored

examples show faults on the end of transmission lines with values of ground current flowing before and after the adjacent breaker to the bus has opened for conditions with and without the consideration of mutual induction. All currents shown are equivalent amperes at 115 kv.

Figure 5 shows the effect of mutual induction between the 115-kv and 230-kv lines for a single-phase-to-ground fault on the station *B* 230-kv bus. When mutual induction is considered (Figure 5A), the calculated value of ground current in the 115-kv line is 205 amperes from station *B* to station *A*. However, when the effect of mutual induction is ignored (Figure 5B), the calculations show a flow of only 135 amperes in the 115-kv line, *but in the opposite direction to that shown in Figure 5A*. In this case the 115-kv line might be cleared by relay unnecessarily for a 230-kv line fault if one were not aware of the effect of mutual induction between paralleling lines of different voltages.

It is possible, as shown in Figure 6, to have appreciable ground currents flowing in sections isolated from the fault because of mutual induction between circuits. The effect shown in Figure 6 would be much more pronounced with longer parallels or larger concentrations of ground currents. Figure 6A shows the magnitude and direction of the ground currents circulating between stations *A* and *B* resulting from the effect of mutual induction when a fault occurs on a radial 230-kv line fed from station *C*. If mutual induction is ignored, results will be obtained similar to those shown in Figure 6B. In the latter case practically no current circulates in the lines between stations *A* and *B*.

The effect of mutual induction increases the over-all ground current flowing in parallel lines providing the currents flow in opposite directions. Figure 7 shows this effect very clearly on the station *A* to station *B* 115-kv line which parallels the 230-kv line for 35 miles. The mutual induction (Figure 7A) increases the ground current in the line and the station *B* transformer neutral practically fivefold over the values obtained in Figure 7B, which neglects mutual induction, for the same 230-kv ground fault. This is an instance that could very easily cause false relay operation, since erroneous results would be obtained by neglecting the mutual induction. Proper results could not be obtained on a d-c board since the lines are of different voltages.

Conclusions

A d-c calculating board cannot be used to represent the effects of mutual induction between lines operated at different voltages. The neglect of these effects may give erroneous answers serious enough to result in false relaying.

With two lines paralleling each other on the same right of way through which unbalanced fault current is flowing in the same direction, the effect of mutual induction between the lines is to reduce the flow of ground current.

The effect of mutual induction between two lines paralleling each other on the same right of way through which unbalanced current is flowing in opposite directions is to increase the flow of ground current.

It is possible, because of the effects of mutual induction between lines, to reverse the normal direction of the ground current flowing in the grounded neutral of a transformer bank to a ground fault.

Appendix. Formulas for Calculations of Zero-Sequence Impedances of Short Transmission Link Without Ground Wires

Zero-Sequence Impedance

$$Z_0 = R_c + 0.00477f + j.01397f \times \log_{10} \frac{D_e}{GMR \text{ circuit}}$$

(1) (Reference 1, page 157)

Z_0 = ohms per phase per mile

$$Z_m = 0.00477f + j.01397f \times \log_{10} \frac{D_e}{GMD \text{ circuit}}$$

(1) (Reference 1, page 158)

Z_m = ohms per phase per mile

f = frequency in cycles

D_e = 2,790 feet based on an average ground resistivity of 100 meter-ohms
(1) (Reference 1, pages 146–8)

$$GMR \text{ circuit} = \sqrt[3]{GMD \text{ sep}^2 \times GMR \text{ conductor}}$$

(1) (Reference 1, page 157)

GMR conductor obtained from tables
(1) (Reference 1, page 138)

$$GMD \text{ sep} = \sqrt[3]{\text{three distances between conductors}}$$

R_c = resistance per mile of one conductor

$$GMD \text{ circuit} = \sqrt[9]{\text{nine distances between conductors}}$$

Conversion of ohms to per unit Z

$$\text{Per unit } Z = \frac{\text{base mva} \times \text{ohms}}{\text{kv}^2} \qquad (2)$$

For mutual impedance between lines of different voltages

$$\text{Per unit } Z = \frac{\text{base mva}^* \times \text{ohms}}{\text{kv}_1 \times \text{kv}_2}$$

References

1. Symmetrical Components (book), C. F. Wagner, R. D. Evans. McGraw-Hill Book Company, Inc., 1933.

2. Calculation of Short-Circuit Currents in A-C Networks, W. M. Hanna. *General Electric Review*, April 1937, pages 189–96.

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* Megavolt-amperes.

High-Voltage-Ignition-Cable Design for Aircraft

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IN the design of an insulation, mechanical and chemical problems must be considered as well as the electrical problem. This is particularly true in the design of an ignition cable for aircraft.

Before constructing a suitable insulated wire or cable for any purpose, it is essential to know the conditions of usage. An aircraft ignition cable may be subjected to the following:

1. Abrasion and chafing.
2. Pulling.
3. Compression.
4. Vibration.
5. Heat.
6. Cold.
7. Moisture.
8. Oil and solvents.
9. Ozone.
10. Nitric acid.
11. Low pressures.

In addition, the insulation must have high dielectric strength and low capacitance.

The cable is subjected to abrasion, chafing, and pulling during assembly into the manifold and flexible conduit leads. This is emphasized by the close fitting manifold, made in an effort to save space and weight. Insufficient space is an enemy of insulation. Many engineers often neglect to allow enough space to prevent injury to insulations. It is appreciated that weight and space saving are very important, especially in aircraft, but proper functioning and safety should take precedence over space saving.

Compression of the cable takes place at the tightly fitting grommets within the spark plug and elbow fittings.

Vibration is quite severe while the engine is running.

According to aircraft-engine manufacturers, and engineers of the Air Services, the temperature in the spark-plug elbow may at times reach 325 to 350 degrees Fahrenheit. The temperature at the spark-plug gasket is 400 to 500 degrees Fahrenheit, and the spark-plug barrel or well may reach a temperature of 375 to 400 degrees Fahrenheit.

We are told that temperatures as low as −70 degrees Fahrenheit or even lower are encountered in high altitude flying and reach this at ground level in parts of the world where our planes are in operation. Although the cables are not flexed or moved at these temperatures, spark plugs may be changed at −20 degrees Fahrenheit or even lower. In such event the cable is flexed or bent to some extent.

Although the cable is entirely enclosed in shielding, moisture enters the system because of breathing, and condensation may take place. If moisture is absorbed by any part of the cable, arcing and premature failure may occur.

The cable comes in contact with lubricating oil and with solvents which are used to wash down and clean the engine.

The cable is exposed to the effects of corona. The ozone formed because of the high electrical stress on the air surrounding the cable is a powerful oxidizing agent. The effects of ozone on the cable can be observed internally next to the conductor as well as externally.

Ozone combines with nitrogen of the air to form oxides of nitrogen. These oxides form nitric acid in the presence of moisture. Nitric acid is also a very powerful oxidizing agent.

High-altitude flying has greatly complicated the problem of designing satisfactory aircraft ignition cables. Cables that function reasonably well at moderately low altitudes fail after relatively short service at very high altitudes.

So far as can be learned, the peak voltages impressed on the cables are in the magnitude of from 7.5 to 10.0 kv.

The dielectric constant of the insulation should be such that the capacitance of the cable will be low enough not to interfere with proper firing of the spark plug.

Consideration of the conditions of service makes it clear that designing a cable that will function satisfactorily is no easy problem. The conventional type of rubber-covered, braided, and lacquered cable is basically lacking in certain respects. The lacquer coating is deficient in resistance to abrasion and chafing. It is difficult, if not impossible, to moistureproof the braid completely. The lacquered braid is readily attacked by the combined action of heat and nitric acid, particularly in the confined space in the spark-plug well and elbow. Furthermore, the lacquer coating when subjected to heat, especially at high altitudes, is seriously affected.

The primary purpose of the lacquer coating is to prevent ozone deterioration of the rubber compound. If the lacquer coating is ruptured, electrical failure is likely to result in a relatively short time.

It is apparent that there is a need for an improved cable, especially for high-altitude flying. The cable designer must therefore find more suitable insulating materials to produce a cable which will meet the requirements. It is also apparent that there must be a meeting of minds of the cable manufacturers, the harness manufacturers, the aircraft-engine manufacturers, and the engineers of the Air Services on certain compromises. It is unlikely that a cable can be made that will meet all the conditions enumerated previously to the extent desired by each individual engineer. One engineer may emphasize heat resistance, another cold resistance, or some other requirement. This emphasis on any specific property can only be met at the expense of some other equally important requirement. What we must arrive at is a balance of properties so that we will obtain a cable with the maximum of any one characteristic without undue reduction of any other necessary characteristics.

The present trend in specification writing is to emphasize performance tests rather than constructional details or requirements based on specific materials. Very often specifications which are not based on performance will prevent the development of improved products. This can occur when new materials become available which will meet conditions of service more satisfactorily but may fail to meet tests based on specific materials. Performance specifications on the other hand encourage development, use of new materials, and better design. It is more difficult to prepare performance specifications since the object is to simulate conditions of service with a reasonable margin of safety. However, when such specifications are finally achieved, it will usually procure a product more satisfactory for the intended purpose.

It is unusually difficult to determine the suitability of an aircraft-engine igni-

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tion cable. It is at best a long procedure to make power-plant tests, and heretofore laboratory tests have revealed comparatively little as to how the cable would perform in actual flight.

Ignition-cable manufacturers have had this under consideration for some time and have attempted to prepare performance specifications that would reproduce as closely as possible conditions actually encountered in service. One test method suggested for incorporation in a specification is termed "simulated flight test." Figure 1 shows this apparatus. It consists principally of a bell jar mounted on a base plate, with proper fittings for attachment of a spark-plug chamber, a spark plug and elbow assembly with a length of standard flexible conduit. A suitable heater coil is mounted around the elbow and spark plug. The bell jar is partially evacuated to the desired pressure. The sample of cable to be tested is assembled in the flexible conduit lead, and the assembly at the spark plug is made identical with that made on an actual engine installation.

These test conditions have been suggested and may be modified as more experience and data are obtained in testing with the apparatus:

Gasket temperature	500 ± 10 degrees Fahrenheit
Elbow temperature	325 ± 5 degrees Fahrenheit
Pressure in electrode chamber	20 ± 5 pounds
Absolute pressure in vacuum chamber	10 ± 0.5 centimeters mercury
Peak voltage	12.5 ± 0.5 kv
Added capacitance	250 micromicrofarad

An attempt has been made to simulate flight conditions by cycling as shown in the tabulation:

Time	Heat	Vacuum	Voltage
Start	On	On	On
3/4 hour		Off	
1 1/2 hours		On	
2 hours	Off		
2 1/4 hours		Off	
3 hours	On	On	On

The voltage is on continuously, and the schedule is continued a definite number of cycles or until failure occurs.

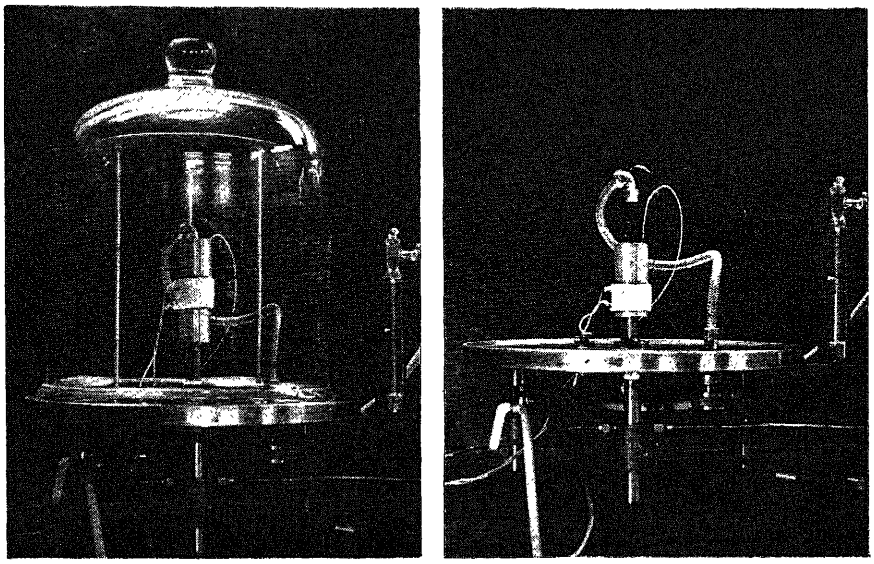

Figure 1. "Simulated flight test" apparatus with and without bell jar

The cycling is shown graphically in Figure 2.

This test method has been in use by cable manufacturers, and a great deal has been learned about faults and deficiencies of present types of cables. It has also been very useful in the testing of new designs.

Aircraft-Ignition-Cable Design

CONDUCTOR

The size of the conductor materially affects the capacitance of the cable, and its cross-sectional area should therefore be as small as possible, consistent with sufficient tensile strength for manufacturing and normal handling. The conductor commonly used for aircraft ignition cable is made with seven strands of 0.013-inch diameter corrosion resisting steel.

The conductor should be smooth, and the stranding should be concentric. This type of stranding most nearly approaches the form of a solid conductor. Any accentuated unevenness or roughness tends to concentrate the electric stress and promote puncture.

It has been found advantageous to use a "sealed" conductor in the construction of aircraft ignition cables to prevent air leakage through the conductor. Leakage occurs at high-altitude flying when the pressure in the spark-plug well is greater than that of the surrounding atmosphere. In case a sealing compound is used in the spark-plug well, it is apt to be forced into the cable along and through the interstices of the cabled conductor. The presence of air inside the cable will also result in the formation of ozone which may lead to internal failure.

Table I. Insulations

Material	Dielectric Strength	Resistance to Compression	Heat Aging	Ozone Resistance	Low Temperature Flexibility	Capacitance
Rubber	1	1	2	4	1	1
Buna *S*	1	1	1	4	2	1
Butyl rubber	1	2	1	2	3	1
Polyisobutylene	1	2	1	3	3	1

The sealing of the conductor results in better adhesion between the conductor and the insulation, thus preventing the insulation from sliding on the conductor. A sealed conductor tends to reduce twisting or dislocation of the conductor which may be the cause of internal corona cracking because displacement of the conductor with respect to the insulation will permit air to enter the cable along the conductor. Twisting or dislocation places the insulation under stress. Stressed insulation is readily attacked by ozone, resulting in cracking and ultimate failure.

The sealing of the conductor is accomplished by extruding or coating the strand of wire used in the center of the cabled conductor with a suitable compound. When the other six strands are cabled around the coated wire, the compound is forced between the individual strands, completely sealing the conductor.

INSULATION

If a sealed conductor is used, a properly designed rubber compound is satisfactory, since the effect of internal corona is eliminated. Rubber compounds have advantages in that the low temperature flexibility is excellent and electrical properties are good. The rubber stock pile is being rapidly reduced, and it may become necessary to find a substitute. Some of the synthetic rubbers appear to be suitable, and among those are the buna *S* and isobutylene types. Cables have been made with these substitutes, and tests indicate that they are quite satisfactory.

In Table I are listed materials which have been used or which are under investigation. It is assumed that the material is properly compounded for the application. The materials are rated in accordance with their comparative value as insulation for ignition cable. The lowest number indicates the greatest value.

PROTECTIVE COVERING

A lacquer coating applied over a braid is used to prevent deterioration of the insulation by ozone, oils, and solvents. It is lacking in resistance to abrasion and chafing. The lacquered braid does not have sufficient resistance to heat and nitric acid. It is, therefore, necessary to use a more suitable protective covering.

Table II. Protective Coverings

Material	Resistance	Moisture Resistance	Heat Resistance	Ozone Resistance	Abrasion and Chafing Resistance	Low Temperature Flexibility
Buna *N'S*	2	1	1	3	1	1
Neoprene	2	1	1	2	1	1
Thiokol	1	1	3	1	1	1

In Table II are shown some of the materials that have been investigated and applied as protective coverings for ignition cables. It is assumed that the material is properly compounded for the application. The materials are rated in accordance with their comparative value as protective covering. The lowest number indicates the greatest value.

Most of the failures of ignition cable on aircraft engines occur in the spark-plug well or spark-plug elbow. This is readily understandable, since the cable is subjected to intense ozone concentration at this point as well as nitric acid formed by the combination of ozone, nitrogen, and moisture. The temperature is also highest at this location. Heat combined with low pressure tend to cause blistering or swelling of the protective covering. With lacquered cable, blistering might occur on the first flight at very high altitudes. The condition can be readily reproduced in the "simulated flight test."

The disintegrated lacquer seems to contaminate the porcelain or other type of sleeving placed over the end of the cable as well as the insulating wall of the spark plug. This contamination may induce arc-over from the spring contact to the outside casing of the spark plug. The arc-over is most likely to occur at high altitudes, particularly if the Neoprene grommet does not completely seal the spark-plug well.

The most satisfactory material for a protective covering at present appears to be suitably compounded Neoprene. Neoprene sheaths are reasonably ozone resistant and are improved by incorporating into the compound antioxidants and waxy materials that tend to migrate to the surface. A coating of wax applied to the cable will also improve corona resistance.

Neoprene-sheathed cables have good low temperature flexibility and will withstand flexing at −40 degrees Fahrenheit. These cables should not be bent or flexed at lower temperatures. This resistance to low temperature is better than that of lacquered cables which should not be flexed at temperatures lower than −20 degrees Fahrenheit.

As previously stated, heat and low pressure existing at high altitudes tend to cause blistering or swelling of the Neoprene sheath at the spark-plug elbow. This exposes the insulation to the conditions in the spark-plug well and elbow.

It has been found that one factor causing blistering of Neoprene-sheathed cable is that the moisture absorbed by the cable in the steam vulcanization process tends to expand because of the temperature at the elbow. At low pressures the tendency to blister is greatly increased. The blistering caused by moisture can be prevented by preheating the Neoprene-sheathed cable for 24 hours at 180 degrees Fahrenheit.

Because of the limited space in the ignition-cable manifold and the increased surface friction of Neoprene-sheathed cables, there is greater mechanical abuse in wiring the harness than when lacquered cable is used. A glass-yarn braid applied between the primary insulation and the Neoprene sheath will impart added mechanical strength to the cable and help prevent stretching and twisting of the cable during manifold assembly. A glass-yarn braid also materially increases ozone resistance of the cable.

One disadvantage of a glass-yarn braid is the relative difficulty of sealing the cable when the pressure surrounding the spark plug and elbow is low as occurs at high altitudes. Air tends to flow through the interstices of the braid which may cause blistering or swelling of the Neoprene sheath at the elbow and lower the pressure in the spark-plug well. The spark-plug-well sealing compound may be forced up into the braid. This tendency of leakage through the braid can be partially overcome by applying a thin sheath of Neoprene over the insulation.

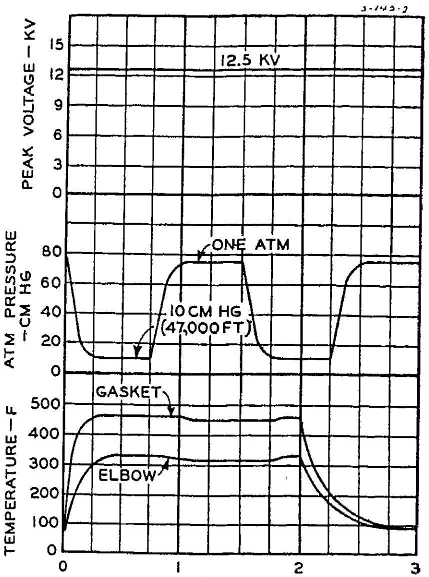

Figure 2. Graphs showing heat, atmospheric pressure, and voltage conditions during "simulated flight test"

Table III. Relative Merit of Cables

	Braided With Lacquered Coating	Neoprene Sheath No Braid	Neoprene Sheath Glass-Yarn Braid Over Insulation	Neoprene Sheath Glass-Yarn Braid Between Layers of Neoprene
Abrasion and chafing	Poor	Excellent	Excellent	Excellent
Pulling	Fair	Poor	Excellent	Excellent
Compression	Poor	Fair	Fair	Fair
Heat	Fair	Good	Good	Good
Cold	Fair	Good	Good	Good
Moisture	Fair	Excellent	Good	Good
Oil	Excellent	Good	Good	Good
Aromatic solvents	Poor	Fair	Fair	Good
Ozone	Excellent	Good	Good	Excellent
Nitric acid	Poor	Excellent	Excellent	Excellent
Low pressure	Poor	Excellent	Fair	Good

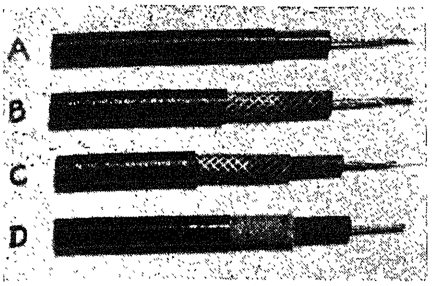

Figure 3. Four ignition-cable constructions

A. Sealed conductor; insulation; Neoprene sheath

B. Sealed conductor; insulation; glass-yarn braid; Neoprene sheath

C. Sealed conductor; insulation; Neoprene layer; glass-yarn braid; Neoprene sheath

D. Conductor; insulation; cotton yarn braid; lacquer coating

The glass-yarn braid is then applied, followed by a second sheath of Neoprene. The braid thus becomes thoroughly imbedded between the layers of Neoprene. This construction further increases the ozone resistance of the cable.

Figure 3 shows constructions which have been discussed. Figure 4 shows deterioration of lacquered-type cable at spark-plug elbow after being subjected to "simulated flight test" and after service on aircraft engine.

Use of Five-Millimeter Cable in Place of Seven-Millimeter Cable

In the wiring of the conventional type of manifold, the seven-millimeter cable receives considerable abuse. Undue pulling or twisting of the ignition cable in harness wiring may occur, resulting in very serious permanent injury to the insulation. Twisting of the cable is apt to produce air pockets along the conductor because of dislocation of the insulation with respect to the conductor. Excessive pulling on the cable may result in elongation of the conductor and stressing of the insulation. The Neoprene-sheathed cable is more resistant to abrasion and chafing than braided and lacquered type, but nevertheless must be handled with reasonable care. Because of the limitations of space, it is doubtful whether seven-millimeter Neoprene-sheathed cable can be assembled in the present manifolds without injury to the cable. The use of five-millimeter cable in place of seven-millimeter cable would simplify the problems of harness assembly and remove the principal source of cable abuse.

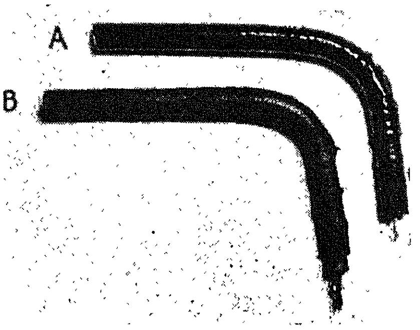

Figure 4. Samples of lacquered cable showing deterioration at spark-plug elbow

A. After "simulated flight test"

B. After service on aircraft engine

Since the puncturing voltage of the five-millimeter Neoprene-sheathed cable is ample, and since cable failure is largely due to deficiencies in chemical and physical properties, it appears that this cable would be entirely satisfactory. The capacitance of an installation with five-millimeter cable is satisfactory since with present manifold and flexible conduit sizes and the same type of cable, the capacitance of the individual leads is less than when seven-millimeter cable is used because of the additional air space in the manifold. This favorable condition would only exist so long as the manifold size remains the same or is reduced only to a point that ample space is allowed to prevent mechanical injury to the insulation of the five-millimeter cable. Should the manifold size be reduced to fit the five-millimeter cable as closely as the present manifold fits the seven-millimeter cable, the same objectionable difficulties in assembly will exist.

Four types of cables have been under consideration. None of these cables meet all of the requirements to the extent desired. Any one is superior in some respects and deficient in other respects. It must be recognized that some of the requirements asked for by aircraft-engine manufacturers and engineers of the Air Services cannot be fully met. As an example, if it is essential to have a cable withstand the solvent action of highly aromatic gasolines, none of the cables under consideration would be entirely satisfactory. Again, if flexibility much below −40 degrees Fahrenheit is required, these cables would not be suitable.

In Table III, an attempt has been made to estimate the relative merits of the four cable constructions under consideration as to the conditions of usage on engine installations previously outlined.

Cable manufacturers at present are doing intensive development work in an effort to improve aircraft ignition cables. The numerous new materials which are being made available by the chemical industry are being thoroughly investigated, and advantage is being taken of those showing promise. Recent close cooperation between the engineers of the Air Services, the aircraft-engine manufacturers, and the cable manufacturers has greatly accelerated the development and improvement of ignition cables, and this will no doubt result in continued progress.

Dethermalizing Arc Quenchers

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Synopsis: The primary function of the normal-type oil circuit breaker is to interrupt the circuit as rapidly as possible, without disturbing the connected system, and to leave the oil circuit breaker in substantially the same electrical and mechanical condition as before. The interruption of an electrical circuit is always accompanied by an arc, and it has been found that if the arc and its associated space are rapidly cooled or dethermalized it is readily extinguished. This paper describes dethermalizing arc quenchers that cause the arc and its associated space to be cooled by the controlled flow of oil through and around the arc. The action of these quenchers has been verified by field tests on the 16-kv and 220-kv systems of The Southern California Edison Company Ltd.

IN the operation of large electric systems, effort is continuously exerted to gain reliability and at the same time to cut down maintenance costs. One of the costly maintenance problems is the overhauling of oil circuit breakers. It is the customary rule, based on experience, to overhaul oil circuit breakers after each short circuit or at least after two interruptions of short circuit. The overhaul periods of the oil circuit breakers could be greatly lengthened if the contact burning and the oil carbonization could be reduced. In order to accomplish these desired results it is important to cut down arcing time and contact separation during the arcing period.

Conclusion

The proper design of dethermalizing arc quenchers will interrupt short circuits of any magnitude without carbonizing the oil or changing its dielectric value, and because of the short arcing time the burning of the contacts becomes negligible.

Even though this short arcing time is accompanied by an extremely short contact separation, the continuously flowing oil prevents restriking of the arc. Consequently, with short arcing time and short arc lengths the arc energy is so greatly reduced that mechanical disturbances are eliminated.

The field tests on the Southern California Edison Company Ltd. system proved that the dethermalizers are adequate to interrupt short circuits ranging from a minimum to maximum duty without mechanical disturbance to the switch and without electrical disturbance to the system. The time of interruption decreased with the greater duty, thus giving the switch a larger factor of safety or an increased kilovolt-ampere rating.

Tests were made on the Edison system at widely separated locations, resulting in the same interrupting characteristics; that is, with the same kilovolt-ampere duty, the arcing time and contact separation were substantially the same, proving that the dethermalizers are adequate for all locations on the Edison system.

The record of operation of these dethermalizers during the past six or seven years has shown no deterioration of oil contacts, or insulating materials of the interrupters, and they have cleared all short circuits and line dropping incident to normal operation.

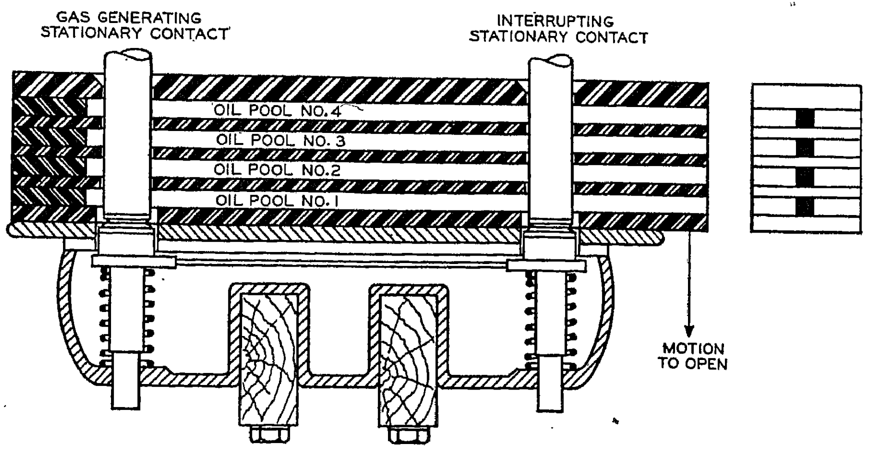
From this operating experience it becomes apparent that this type of arc quencher will result in the very minimum of maintenance and at the same time will give a maximum of system protection.

16-Kv Field Tests, August 25, 1936

Since the Edison company is practically the only company that uses 16 kv for distribution, it was desirable to have a breaker for this voltage with approximately 50,000-kva interrupting rating, small in size and fast in operation. Breakers of the normal type had insufficient interrupting capacity and required considerable maintenance. The dethermalizer for this breaker is shown in Figure 1. It can be seen from this sketch that the dethermalizing element has a multiplicity of pools of oil that are connected to restricted ports on one end, the other end is closed. The arc at the closed end generates gas which produces sufficient pressure to drive the oil in the pool, located between the contacts, through the arc and associated space of the other contact. As the contacts separate, other pools of oil are successively driven into the arc and the arc space of the second contact thus continually cooling the arc and the arc space until a point of extinction occurs, and sufficient cool oil is maintained through this space even after extinction so that restriking does not occur. This rapid cooling of the arc and its associated space is the dethermalizing action that extinguishes the arc.

Figure 2 illustrates a 16-kv 50,000-kva series-trip mechanical trip-free breaker set up for test. This breaker interrupted a 53,000-kva three-phase short circuit on a delta system feeding only the setup from a separate transformer bank. It operated on a close–open test in $3^{1}/_{2}$ cycles total time. Figure 3 shows the oscillogram of this test. This switch was closed by hand on a three-phase short circuit and cleared with no disturbance. This test was repeated several times with the same results and when examined showed very little deterioration of contacts, oil, and dethermalizer. Breakers of this design have an operating record of six and one-half years, show no signs of oil carbonization, and have not required any maintenance.

Figure 1. 16-kv dethermalizer, flat-plate construction



Paper 43-142, recommended by the AIEE committee on protective devices for presentation at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943. Manuscript submitted June 18, 1943; made available for printing July 20, 1943.

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The authors wish to acknowledge the assistance of their associates in the Southern California Edison Company Ltd. and Kelman Electric and Manufacturing Company.

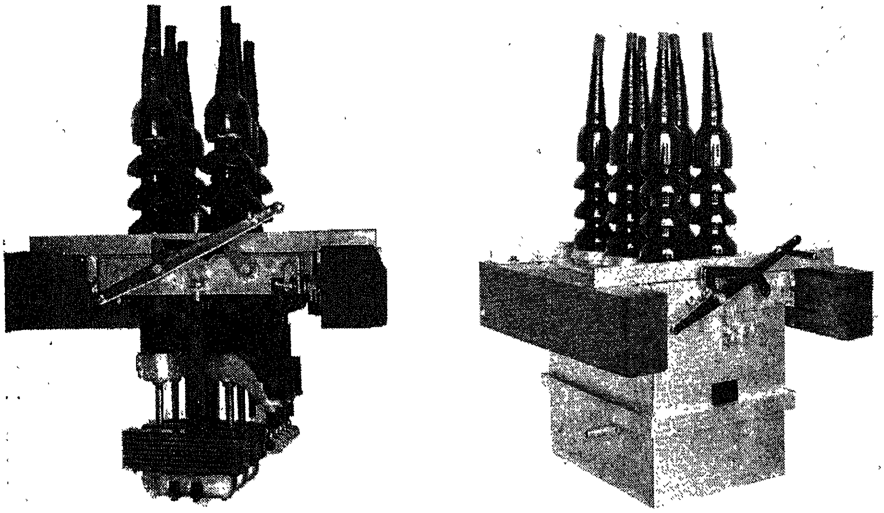


Figure 2. 16-kv oil circuit breaker with dethermalizers set up for field test

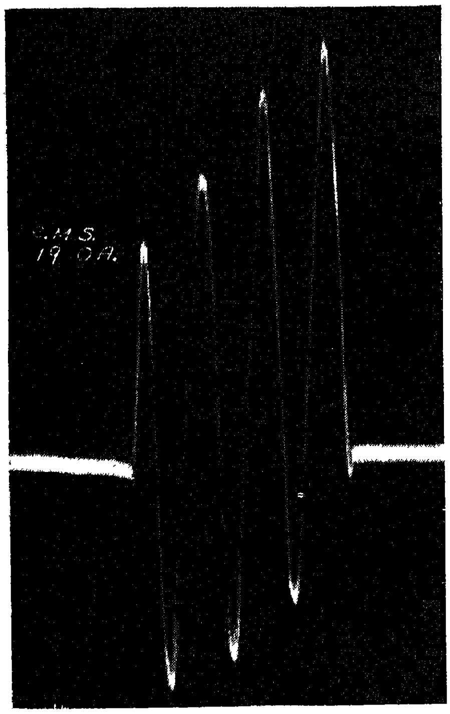


Figure 3. 16-kv close–open test, 53,000-kva three-phase short circuit

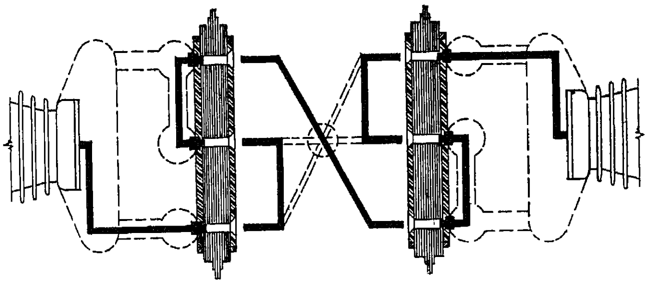


Figure 4. Arrangement of six-break dethermalizers

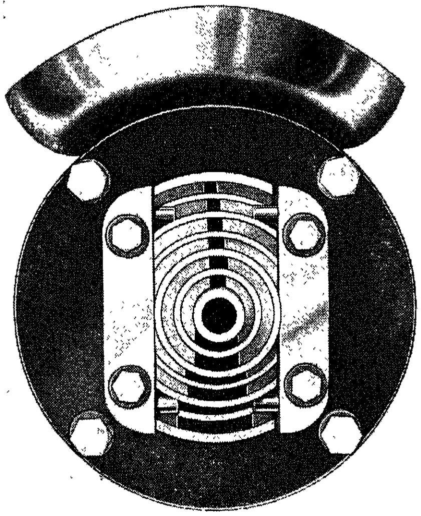


Figure 5. End view of 230-kv tube-type dethermalizer

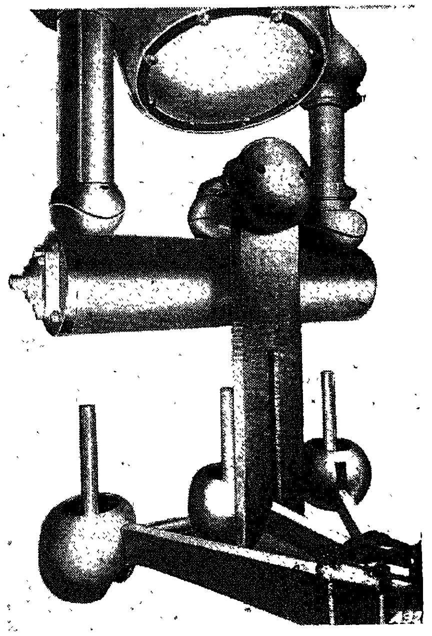
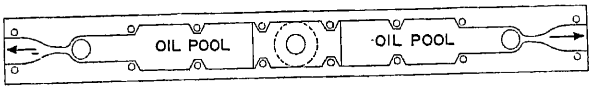


Figure 6 (right). Assembly of 230-kv dethermalizer on switch bushing



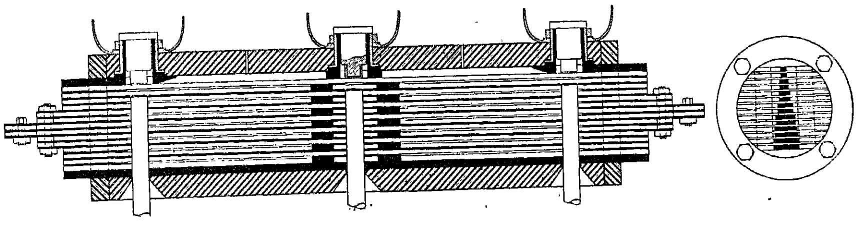


Figure 7. Type *H* dethermalizer flat-type construction, 230 kv

230-Kv Dethermalizers

The dethermalizers for the 230-kv oil circuit breakers are made in the form of six breaks per pole or three breaks per bushing. The electric connections and sequence of these breaks are shown by Figure 4. The dethermalizers are made in the form of large tubes. Figure 5 shows the end view of the tube type. The graded ports can be seen in this figure. Each dethermalizer is mounted on the end of the switch bushing as shown by the assembly photograph, Figure 6. This illustration clearly shows the shielding and one-half the crosshead.

Figure 7 shows the dethermalizer of the flat-type construction and the various oil pools or reservoirs. The end view shows the graded ports and the plan view shows the restriction of the ports.

When the switch contacts are parting, arcs are formed at each contact. The arc at the center contact will generate sufficient pressure to force the oil in the pools (located between the center contact and the outside contacts) through the outside arcs and through the ports at each end of the large tube. As the contacts are opening, new pools of oil are successively forced through the outside

Table I. Tests Made at Saugus Substation, October 4, 1936

Kelman Dethermalizing Arc Quenchers, Type As Indicated

Test Number	Initial Voltage Phase to Phase (Kilovolts)	Type of Quencher Tube	Number of Breaks Per Phase	Phase Tank	Duty Cycle	Current Interrupted (Rms Amperes)	Equivalent Three-Phase Kilovolt-Amperes Interrupted	Closing Time (Cycles)	Over-all Time of Short Circuit (Cycles)	Relay Time (Cycles)	Breaker Inter-rupting Time (Cycles)	Contact Separation When Arc Extinguished (Inches)	Total Arc Length (Inches)
1	220	*C*	6	*A*	*O*	778	296,470		8.0	1.0	7.0	$3^1/_8$	$18^3/_4$
1*A*	220	*C*	6	*A*	*CO*	883	336,480	.65	7.0	1.0	6.0	3	18
1*C*	220	*	6	*C*	*O*	787	299,897		6.5	1.0	5.5	$2^1/_4$	$13^1/_2$
1*AC*	220	*	6	*C*	*CO*	777	296,090	.67	9.0	1.0	8.0	$3^1/_2$	21
2	220	*C*	6	*B*	*O*	1,162	442,800		6.5	0.75	5.75	$1^3/_8$	$8^1/_4$
2*A*	220	*C*	6	*B*	*CO*	1,171	446,225	.68	6.5	1.0	5.5	$1^3/_8$	$8^1/_4$
3	220	*C*	6	*B*	*O*	Not recorded because of oscillograph film choke. Same kilovolt amperes interrupted as test 3A							
3*A*	220	*C*	6	*B*	*CO*	2,055	783,090	.69	7.0	0.75	6.25	$1^5/_8$	$9^3/_4$
4	220	*C*	6	*B*	*O*	4,142	1,578,363		5.5	0.75	4.75	$1^1/_8$	$6^3/_4$
4*A*	220	*C*	6	*B*	*CO*	4,526	1,730,000	.69	5.5	1.0	4.5	$1^1/_8$	$6^3/_4$
4*A*1	220	*C*	6	*B*	*CO*	4,249	1,619,140	.69	5.3	1.0	4.3	1	6

* In tests 1*C* and 1*AC* the dethermalizers were of the flat-plate construction, and the ports were not graded. All other tests were made with the type *C* dethermalizers which were of the round-tube construction with graded ports.

O—Open. *CO*—Close-open.

Table II. Tests Made at Laguna Bell Substation

Tests Made on July 10, 1938, on Kelman RA6J Breaker, 1,200 Amperes, 230 Kv, Serial Number 16408

Test Number	Initial Voltage Phase to Phase (Kilo-volts)	Type of Quencher Tube	Num-ber of Breaks Per Phase	Tank	Duty Cycle	Current Inter-rupted (Rms Amperes)	Equivalent Three-Phase Kilovolt-Amperes Interrupted	Over-all Time of Short Circuit (Cycles)	Relay Time (Cycles)	Breaker Inter-rupting Time (Cycles)	Breaker Dead Time (Cycles)	Arcing Time (Cycles)	Contact Separation When Arc Extin-guished (Inches)	Total Arc Length (Inches)
Test Made on July 10, 1938, Original Installation, $1^1/_4$-Inch Bayonets and $1^1/_2$-Inch Clearance Holes														
1	210	*A*	6	West	*O*	660	240,000	25.0	0.36	24.64	2.60	22.04	$17^3/_8$	$104^1/_4$
1*A*	210	*A*	6	West	*CO*	630	230,000	12.2	0.48	11.72	3.94	7.78	$5^1/_4$	$31^1/_2$
1_1	210	*A*	6	West	*O*	660	240,000	12.0	0.39	11.61	2.80	8.81	$5^1/_2$	33
1_1*A*	210	*A*	6	West	*CO*	660†	240,000	24.5	0.43	24.07	3.90	20.17	$17^1/_2$	105
Note: Larger values of short-circuit current were not imposed because of the excessive arc length.														
Test Made on July 31, 1938														
1-*C*	210	*C*	6	East	*O*		No record obtained							
1-*C*	210	*C*	6	East	*O*	576	210,000	8.10	0.37	7.73	3.26	4.47	$2^3/_8$	$14^1/_4$
1-*G*	210	*G*	6	West	*O*	576	210,000	8.02	0.40	7.62	2.75	4.87	$2^5/_8$	$15^3/_4$
1-*H*	210	*H*	6	Middle	*O*	573	209,000	7.97	0.36	7.61	2.71	4.90	$2^3/_4$	$16^1/_2$
1*A*-*C*	210	*C*	6	East	*CO*	570	207,000	10.3	0.34	9.95	4.11	5.85	$3^5/_8$	$21^3/_4$
1*A*-*G*	210	*G*	6	West	*CO*	576	210,000	10.47	0.33	10.14	4.03	6.11	$3^7/_8$	$23^1/_4$
1*A*-*H*	210	*H*	6	Middle	*CO*	576	210,000	8.92	0.31	8.61	4.21	4.4	$2^5/_8$	$15^3/_4$
2-*C*	210	*C*	6	East	*O*	1,517	551,000	6.54	0.26	6.28	2.79	3.49	$1^3/_4$	$10^1/_2$
2-*G*	210	*G*	6	West	*O*	1,529	557,000	6.55	0.37	6.18	3.09	3.09	$1^1/_2$	9
2-*H*	210	*H*	6	Middle	*O*	1,520	553,000	6.02	0.30	5.72	2.72	3.00	$1^3/_8$	$8^1/$
2*A*-*C*	210	*C*	6	East	*CO*	1,529	556,000	7.09	0.29	6.80	4.08	2.72	$1^5/_8$	$9^3/_4$
2*A*-*G*	210	*G*	6	West	*CO*	1,525	555,000	8.00	0.34	7.66	4.14	3.52	2	12
2*A*-*H*	210	*H*	6	Middle	*CO*	1,517	552,000	7.68	0.36	7.32	4.06	3.26	$1^7/_8$	$11^1/_4$
3-*C*	210	*C*	6	East	*O*	2,720	990,000	6.55	0.31	6.24	2.98	3.26	$1^5/_8$	$9^3/_4$
3-*G*	210	*G*	6	West	*O*	2,780	1,011,000	6.05	0.28	5.77	3.01	2.76	$1^1/_8$	$8^1/_4$
3-*H*	210	*H*	6	Middle	*O*	2,750	1,000,000	5.04	0.46	4.58	2.79	1.79	$^3/_4$	$4^1/_2$
3*A*-*C*	210	*C*	6	East	*CO*	2,659	967,000	6.59	0.40	6.19	4.17	2.02	$1^1/_4$	$7^1/_2$
3*A*-*G*	210	*G*	6	West	*CO*	2,639	960,000	7.20	0.38	6.82	4.02	2.80	$1^3/_4$	$10^1/_2$
3*A*-*H*	210	*H*	6	Middle	*CO*	2,610	950,000	6.63	0.30	6.33	4.03	2.30	$1^3/_8$	$8^1/_4$
4-*G*	210	*G*	6	West	*O*		No record obtained							
4*A*-*G*	210	*G*	6	West	*CO*	4,720	1,719,000	6.06	0.35	5.71	3.89	1.82	1	6
4*A*-*H*	210	*H*	6	Middle	*CO*	5,075	1,850,000	5.97	0.30	5.67	4.05	1.62	1	6
5-*H**	210	*H*	6	Middle	*O*	6,200	2,259,000	5.48	0.25	5.23	3.26	1.97	$^3/_4$	$4^1/_2$
5*A*-*H**	210	*H*	6	Middle	*CO*	6,070	2,100,000	6.06	0.30	5.76	4.20	1.56	$^7/_8$	$5^1/_4$

* See oscillograms in Figures 11 and 12.

Types of quencher tubes: Type *C*—Round-tube type as tested at Saugus 10–4–36. 1.050-inch bayonets, center clearance-hole $1^3/_{16}$ inch and clearance-holes round-end, $1^3/_{16}$ inch x $1^7/_{16}$ inch.

Type *G*—Flat-plate style, designed to have the characteristics of type *C*, as nearly as practical. 1-inch bayonets, clearance-holes $1^1/_{16}$ inches.

Type *H*—Flat-plate style. 1-inch bayonets, clearance-holes $1^1/_{16}$ inches. Graded ports.

After the test the contacts were found to be slightly burned, and a slight carbon deposit was found on the quencher tubes. A test of the oil showed no visible trace of carbon.

O—Open. *CO*—Close-open.

† Larger values of short-circuit current were not imposed because of excessive arc lengths.

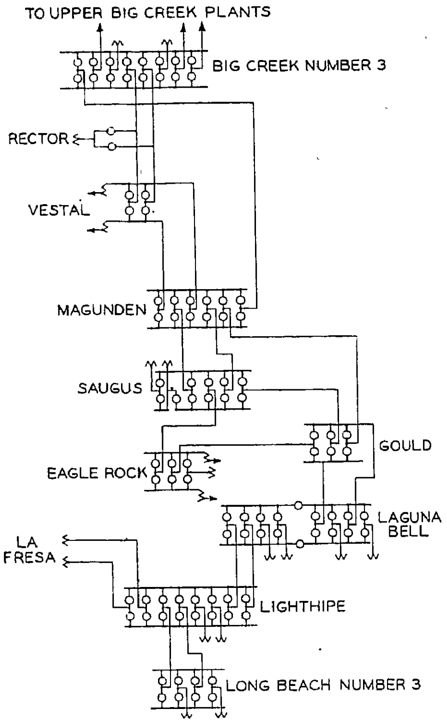


Figure 8. 220-kv system of the Southern California Edison Company, Ltd.

arcs until the circuit is interrupted. At the time of extinction, fresh oil is between the contacts so no restriking occurs. In this arc quencher fourteen ports are used. The tube type operates in the same manner.

Further development has been made on a two-break device which will be the subject of a future paper.

Saugus and Laguna Bell Substations—Field Tests

In order to check the operation of these dethermalizers, actual phase-to-ground short-circuit tests were made on the main 220-kv system at the Saugus and Laguna Bell substations of the Southern California Edison Company Ltd. Figure 8 shows the 220-kv connections of the Edison system at the time of these tests.

The various kilovolt-ampere duties for the switch under tests were obtained by system arrangements. The lowest duty at these two locations was accomplished by feeding 220 kv to 66 kv and then back to 220 kv. This gave the impedance of the two transformer banks in series for limiting the short-circuit kilovolt-amperes. The other steps were accomplished by separating a line from a distant station, which supplied the test breaker. Then by selecting a shorter line the kilovolt-amperes duty was increased, and the final test at the two stations was the maximum kilovolt-amperes that could be delivered at the 220-kv bus, that is, all lines and transformer banks in service. At the Saugus substation this amounted to an equivalent three-phase short circuit of 1,730,000 kva, and at Laguna Bell it amounted to 2,259,000 kva.

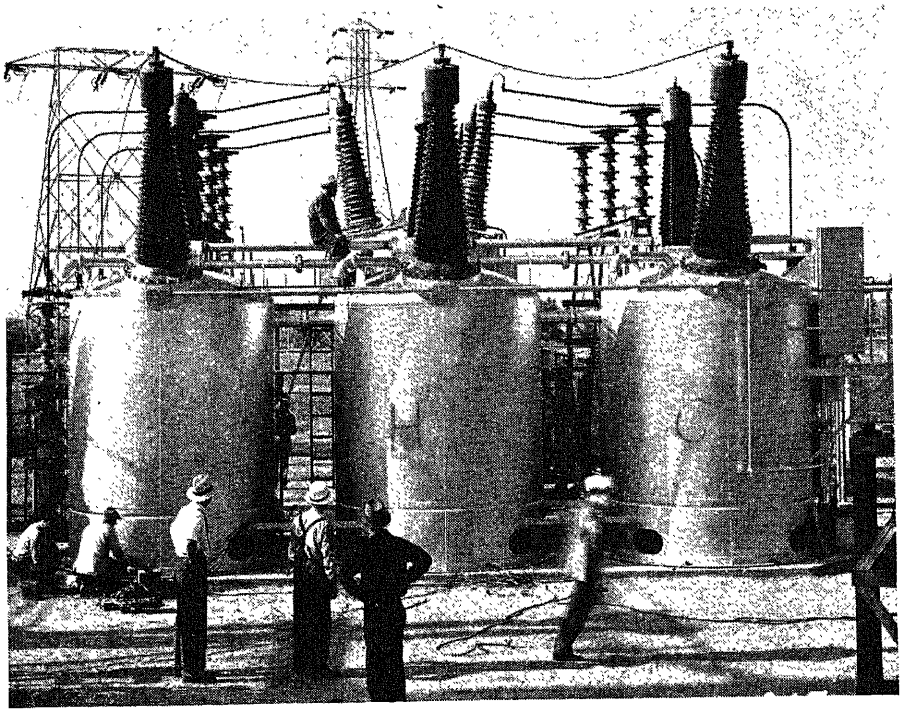


Figure 9. Kelman 230-kv 1,200-ampere 2,500,000-kva oil circuit breaker equipped with dethermalizer

The breakers at both locations performed perfectly throughout the tests without any mechanical trouble or without disturbing the system. After the series of tests the oil was tested and filtered and did not show any perceptible increase of carbon in the oil.

Figure 9 shows the test breaker at Laguna Bell substation. Figure 10 illustrates bars of various lengths that were placed on end on a steel plate between two tanks of the breaker. The mechanical movement of the breaker under test was so small that none of these bars were knocked over.

Table I is the tabulation of the tests taken at Saugus substation on October 4, 1936.

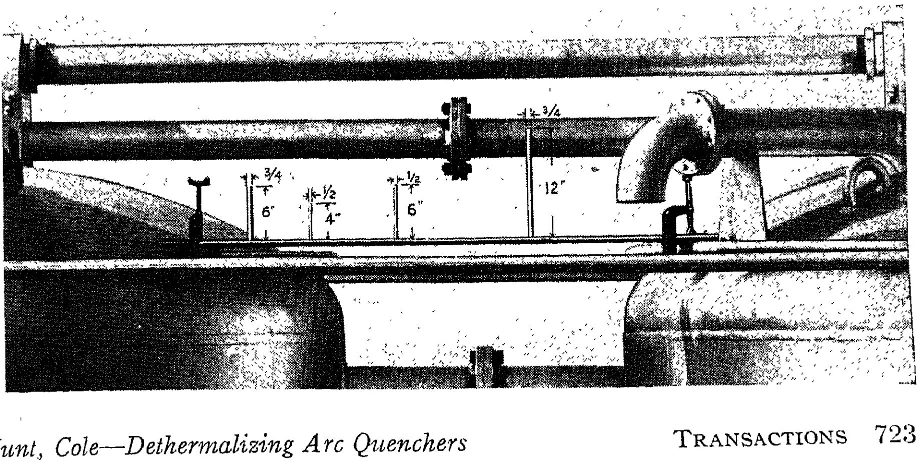
Table II is the tabulation of the tests taken at Laguna Bell substation on July 10, 1938 and July 31, 1938.

In these tests the type *C* dethermalizers are of the tube type, as shown by Figure 5, the *H* type are as shown by Figure 7, and the *A* type are of the flat-plate construction with more restricted ports.

The series of tests on July 10, 1938 were not continued above the values

Figure 10. Rods for determination of switch movement

The four steel pins were set on end to test for any jar to the breaker during opening of short circuits. Pins were turned square on ends and set on a 1-inch by 4-inch steel bar. On 230-kv tests at capacities from 554,000 kva to 2,259,000 kva, throughout a total of 16 tests, none of the pins fell. On the lighter tests, a high-speed moving-picture camera showed slight vibration of the pins, which vibration became less as the duty increased. On the heaviest tests, no vibration was found



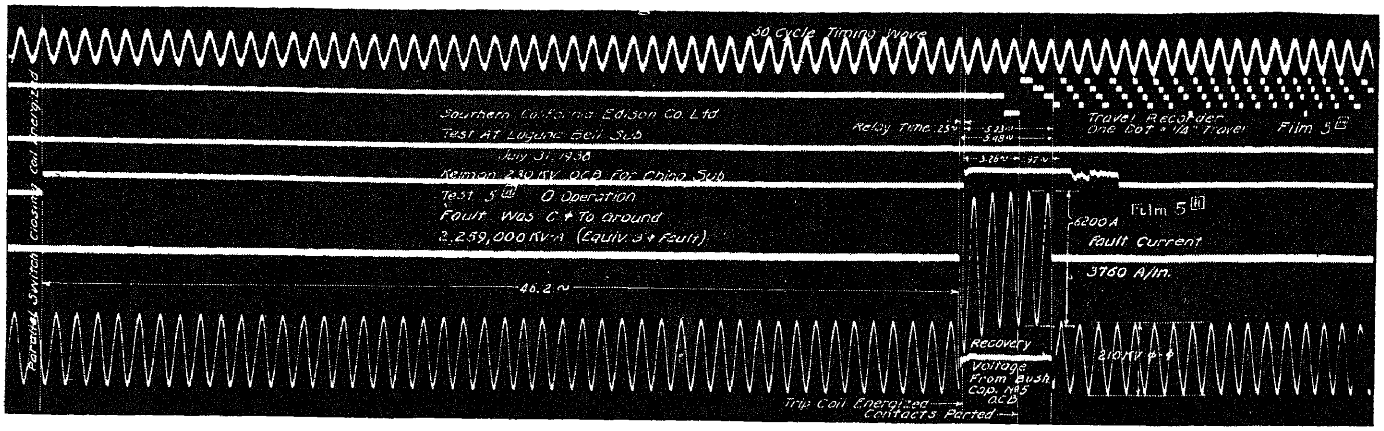
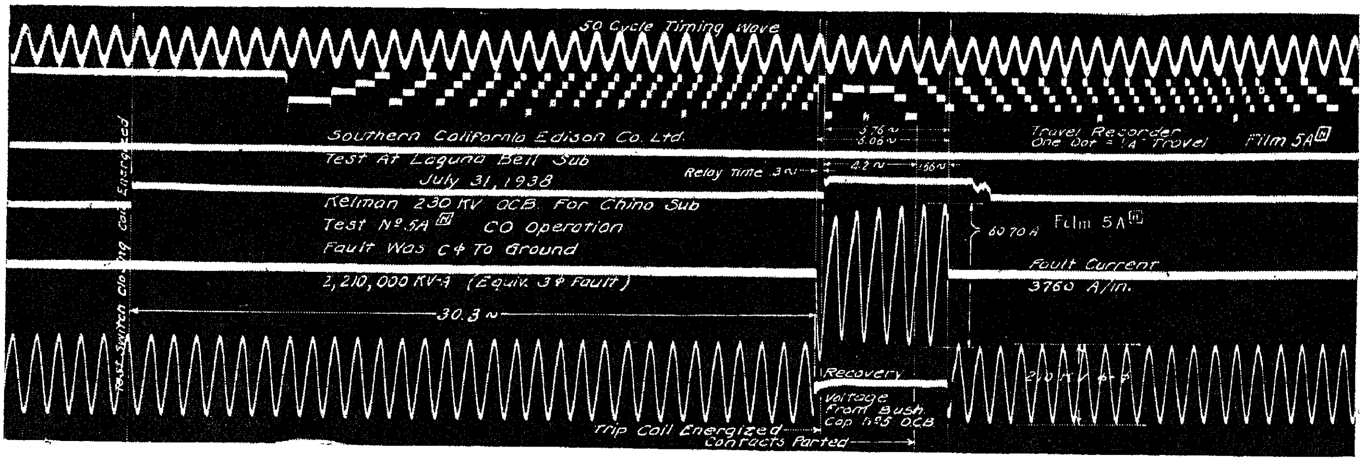


Figure 11 (above). Oscillogram of opening test at 2,259,000 kva

Figure 12 (below). Oscillogram of close–open test at 2,100,000 kva



shown because of the excessive arc length. It was discovered that these dethermalizers had too much clearance around the contacts and the ports were too much restricted, causing the oil to be forced out around the contacts giving practically no quenching effect. This difficulty was remedied, and the tests were completed on July 31, 1938.

It will be noted from these tables that as the duty increased the contact separation decreased at the time of arc extinction.

Oscillograph records were taken of these tests to give an accurate picture of the operation of the breaker. Figure 11 is an oscillogram of an opening operation with a full capacity short circuit at Laguna Bell substation. With the travel recorder it is possible to picture accurately the breaker action. From Figure 11 it can be seen that the arc was extinguished at exactly $^3/_4$-inch contact separation without any restrikes. Figure 12 is an oscillogram of a close–open operation of a full capacity short circuit at Laguna Bell substation. It can be readily seen from this figure that the prestrike was only $^1/_4$ inch, and contact separation at arc extinction was $^7/_8$ inch.

From these oscillograms and tables it should be noticed that dead time of the breaker was excessive. This was later corrected by a change in the tripping toggle.

It can be seen from the two oscillograms that no voltage disturbance occurred at arc extinction.

Since the Saugus tests were the first short-circuit tests to be made on the 220-kv Edison system, every precaution was taken to see that the 220-kv protection was in order and all switching was correct. The dispatching and switching were so well planned and the breaker functioned so perfectly that every test was carried out at the scheduled time without any system disturbance. This successful achievement allowed the privilege of future 220-kv short-circuit tests.

References

1. A New Multibreak Interrupter for Fast-Clearing Oil Circuit Breakers, **R. M. Spurck, H. E. Strang.** AIEE Transactions, volume 57, 1938, pages 705–10.

2. The Vertical-Flow Interrupter and Its Application to "Oil-Poor" Circuit Breakers, **B. P. Baker.** AIEE Transactions, volume 60, 1941, pages 440–5.

3. Some Practical Measuring Devices, **Lloyd F. Hunt.** AIEE Transactions, volume 59, 1940, pages 767–8.

4. Switching Overvoltage Hazard Eliminated in High-Voltage Oil Circuit Breakers, **Lloyd F. Hunt, E. W. Boehne, H. A. Peterson.** AIEE Transactions, volume 62, 1943, February section, pages 98–106.

5. A Multiorifice Interrupter for High-Voltage Oil Circuit Breakers, **L. R. Ludwig, W. M. Leeds.** AIEE Transactions, volume 62, 1943, March section, pages 119–25.

A New One-Cycle Directional Overcurrent Relay

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Synopsis: This paper describes a new one-cycle directional overcurrent relay which is not limited in its application by lack of contact co-ordination. That contact co-ordination is important is indicated by the many incorrect relay operations which have been traced to the common practice of adding instantaneous attachment to directional overcurrent relays. For years it has been known that directional control is not sufficient for instantaneous directional overcurrent relays. This paper describes a new application of "memory action" which for the first time complements directional control to give always correct operation. The new idea is to use "memory action" to obtain a time delay in the operation of the overcurrent unit in the order of one-half cycle. Of course "memory action" is also used in the directional element to retain a high torque level even if the fault causes drastic voltage reduction. Both elements are of the rugged induction cylinder.

HIGH-speed relaying has become one of the most common means of permitting operation of a power system nearer its stability limit. Thus, extension of power transmission facilities has often been obviated. This, together with reduction of damage at the point of fault, has undeniably meant the saving of a large amount of critical material.

An important development in the high-speed relay family has been the distance relay with a stepped time–distance characteristic. With this type of relay, simultaneous instantaneous tripping is obtained for faults in the middle 80 per cent of the line section, and instantaneous tripping at one end for faults in the remaining 20 per cent. This is true over a large range of generating capacity.

For the protection of most of the lower voltage lines, distance relaying has generally been considered to be too expensive. On the other hand, it often occurs that the short-circuit currents of such lines are relatively fixed because the determining impedances are largely in the lines and transformers rather than in the connected generators. Thus, it is often possible to approach distance relay performance by means of instantaneous overcurrent relays. Generally, tripping must be allowed for faults in one direction only, thus necessitating directional action.

Application Requirements

Any directional overcurrent relay, whether instantaneous or time delay, may operate incorrectly while the fault is being cleared or after it has been cleared, if the overcurrent unit is not prevented from closing its contacts when the fault is in the nontripping direction. Fundamentally, the cause of incorrect operation is the loss of a contact race. If the overcurrent unit has operated because of a fault in the nontripping direction, it must open its contacts before the contacts of the directional unit can close, under any condition causing reversal of the directional unit.

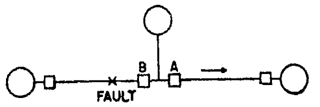

Figure 1. Basic system requiring correct directional action after fault is cleared

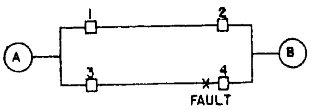

Figure 2. Basic system requiring correct directional action during sequence of circuit-breaker operation

Figure 1 shows a system where the directional overcurrent relay at *A* might trip the circuit breaker at *A* incorrectly after the fault is cleared, if the overcurrent unit is allowed to operate during the fault. If the normal load current is as shown by the arrow, and a fault appears at *X* as indicated in Figure 1, the contacts of the directional unit open, and the contacts of the overcurrent unit close. The instant that circuit breaker *B* opens, the current through the relay at *A* reverses and drops in magnitude. The directional unit closes its contacts quickly because it now has full voltage and load current applied to it. The overcurrent unit, on the other hand, opens its contacts slowly if the magnitude of the load current is near the drop-out value of the overcurrent unit. If the directional-unit contacts close before the overcurrent contacts can open, circuit breaker *A* will be tripped falsely even though circuit breaker *B* has already cleared the fault.

Figure 2 shows a system where a directional overcurrent relay might operate incorrectly during the sequency of circuit breaker operations to clear the fault, because of the fault current reversing when the first circuit breaker opens. For example, when the fault occurs, the overcurrent and directional units at 4 operate. If the generation at *A* is greater than that at *B*, the overcurrent unit at 2 may also operate, but circuit breaker 2 is not tripped because the fault current is in the direction to hold the directional-relay contacts open. When circuit breaker 4 opens, the current through circuit breaker 2 reverses causing its directional relay to reverse. If the directional-unit contacts close before the overcurrent-unit contacts can open, circuit breaker 2 is falsely tripped.

For simplicity, parallel lines were used in Figure 2. The same difficulty may arise in loop circuits.

Instantaneous directional overcurrent relays for phase protection have a contact

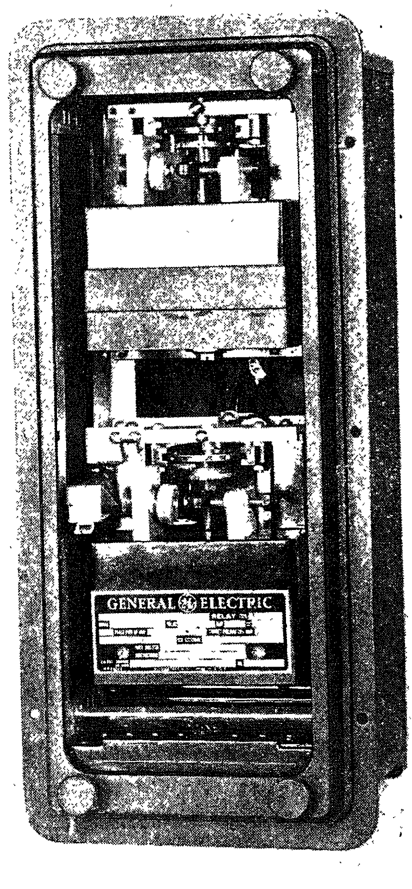

Figure 3. High-speed directional overcurrent relay

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race which is not present either in time-delay directional overcurrent relays or in instantaneous directional overcurrent relays for ground-fault protection. This is the race between the contacts of the directional-unit opening and the overcurrent contacts closing at the inception of the fault. For example, if load in Figure 1 is flowing in the direction of the arrow, the directional-unit contacts at *A* are closed. When the fault appears at *X*, the instantaneous overcurrent-unit contacts of the same relay begin to close. At the same time, the directional unit is attempting to open its contacts because of the reversal of the direction of current flow. Obviously, if the contacts of both units are closed simultaneously during the transition, circuit breaker *A* is tripped falsely.

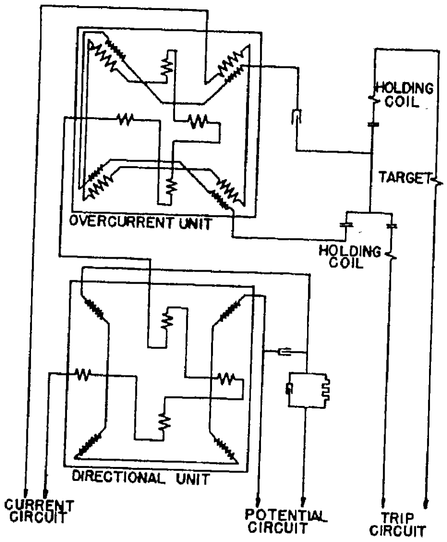


Figure 4. Internal connections of relay

Development of the Relay

In developing a high-speed directional overcurrent relay that operates correctly under any circuit condition, it is necessary to obtain proper contact co-ordination.

Directional control, as in the case of the time overcurrent relay, prevents incorrect tripping either after the fault is cleared or when there is a reversal of short-circuit current during the fault.

To win the more difficult contact race, namely, that occurring when a reverse current fault supplants load current in the tripping direction, it is necessary that the time required for the directional-unit contacts to open be less than the time for the overcurrent-unit contacts to close. This requirement suggests either that the directional unit should be fast even at very low voltage, or that the overcurrent unit should be delayed slightly. Actually, in order to increase the safety factor, both methods are employed. The overcurrent unit is delayed slightly by preventing the immediate rise of torque to its steady-state value. The operating time of the directional relay is decreased, principally at low voltage, by delaying the decay of current in the potential coil, retaining, at the same time, substantially, circuit frequency.

Description of the Relay

The relay consists of an overcurrent unit and a directional unit mounted in a single case. Figure 3 shows the relay, and Figure 4 shows the internal connections. In Figure 3, the upper unit is the overcurrent unit, and the lower unit is the directional unit. Both units are of the eight-pole induction-cylinder construction previously described.[1]

The main windings of the overcurrent unit consist of eight series-connected current coils.

As with all induction devices, the development of torque is dependent upon the difference in time phase between two or more fluxes. (This is not a complete explanation of the torque development of induction devices but is sufficiently explicit for present purposes.) Consequently, four of the current poles, in alternate position, have auxiliary coils. These windings provide a means of controlling or shifting the time phase of the air-gap flux of these poles. The four auxiliary coils, in series, are connected in series with a capacitor and one set of contacts of the directional unit. The torque of the overcurrent unit is proportional to the product of the square of the current and the sine of the angle between the fluxes produced in the shifted and unshifted poles. As long as the contacts of the directional unit are open, the fluxes in all poles are in phase and the torque of the overcurrent unit is zero. When the contacts of the directional unit close, the auxiliary coil circuit of the overcurrent unit is completed. This auxiliary circuit then shifts the phase angle of the flux in the corresponding poles so that it is no longer in phase with the flux in the other poles and the overcurrent unit has a torque in the direction to close its contacts.

The auxiliary circuit is an oscillating circuit, and the build-up of its current is delayed.[2] Since current in the auxiliary winding shifts the flux, a delay of the build-up of this current delays the flux shift. The build-up of current in the auxiliary winding is shown by the trace made by vibrator 1 of the oscillogram shown in Figure 6. This slow build-up

Figure 5. Test oscillogram. Fault in nontripping direction

Vibrator 1—Current in flux-shifting coils of overcurrent unit

Vibrator 2—Line current

Vibrator 3—Current in potential coil of directional unit

Vibrator 4—Voltage across potential circuit of directional unit

Vibrator 5—Current in trip circuit

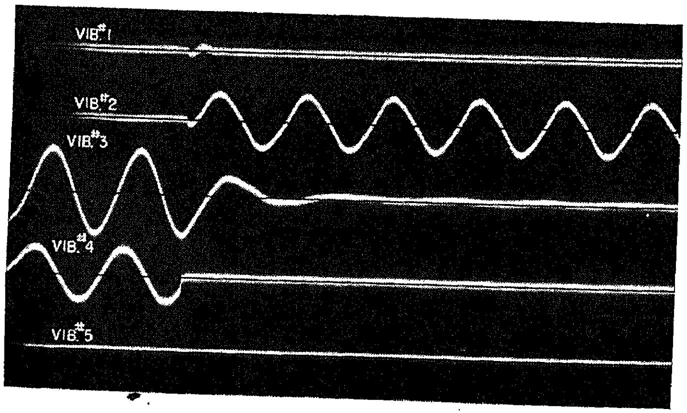


Figure 6. Test oscillogram. Fault in tripping direction

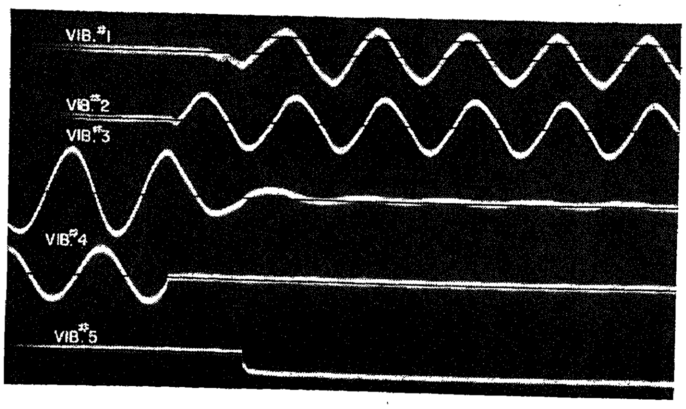
Vibrator 1—Current in flux-shifting coils of overcurrent unit

Vibrator 2—Line current

Vibrator 3—Current in potential coil of directional unit

Vibrator 4—Voltage across potential circuit of directional unit

Vibrator 5—Current in trip circuit



prevents the overcurrent unit from closing its contacts before the directional unit has time to open its contacts.

When the magnitude of the load current is near the pickup value of the overcurrent unit, the impulse that the overcurrent unit receives, under short-circuit conditions, before the contacts of the directional unit can open, will be sufficient to close the contacts of the overcurrent unit. The time required to close the contacts under this condition is greater than the time required to open the contacts of the directional unit, so that the trip circuit is not completed. This has been demonstrated by tests.

The windings of the directional unit consist of four current coils in series and four potential coils in series, mounted on alternate poles. The potential circuit has two capacitors and a resistor in addition to the coils. This potential circuit is also an oscillating circuit so that the flux in the potential coil does not immediately drop when the fault appears. The flux continues to follow system frequency for about one cycle which gives the directional unit ample torque to open its contacts quickly. This slow decay of the potential flux also gives the directional unit enough torque to close its contacts when the fault is in the trip direction even though the voltage across the potential circuit drops to a small value during the fault.

Figure 5 shows an oscillogram taken under the following conditions: Before the fault was applied, the directional relay had full potential. The current was in the direction to close the contacts of the directional unit but below the pickup value of the overcurrent unit. The instant the fault was applied, the current reversed and increased in magnitude to 13 times the pickup value of the overcurrent unit. Concurrently, the voltage across the potential circuit decreased to one per cent of rated potential. Vibrator 3 (Figure 5) shows how the current in the potential coils of the directional unit decayed. Vibrator 1 shows how the current in the auxiliary circuit of the overcurrent unit started to build up but was interrupted by the opening of the contact of the directional unit.

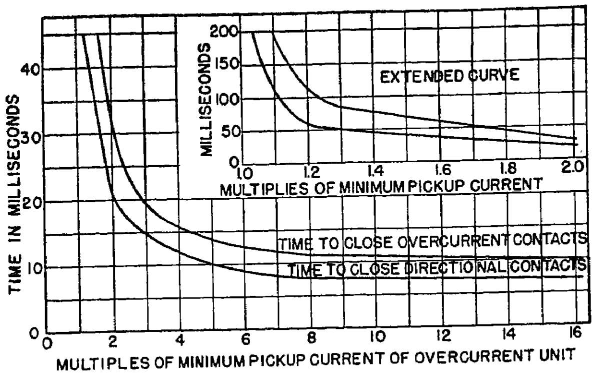


Figure 7. Time–current characteristic

Figure 6 shows an oscillogram in which the conditions were the same as those described heretofore except that the current before the fault was in the nontripping direction and in the trip direction after the fault. This oscillogram shows the build-up of the current of the auxiliary circuit after the directional unit closed (vibrator 1). The oscillogram shows that the contacts of the directional unit closed to complete the auxiliary circuit in approximately one-half cycle (60-cycle basis). Then the overcurrent unit operated to close its contacts in approximately one-half cycle. The trip circuit is therefore completed in approximately one cycle. Since this relay is capable of operating in one cycle, it is believed that it can be called instantaneous, notwithstanding the AIEE definition of "instantaneous," which is "no intentional time delay."

Relay Characteristics

The directional unit of the relay has maximum torque when the relay current leads the relay voltage by 45 degrees. This means that, with the quadrature connection, the relay has maximum torque when the relay current lags the phase-to-neutral voltage of the same phase by 45 degrees. The directional unit will operate correctly at the maximum torque angle with one per cent of rated voltage and the minimum pickup value at which the overcurrent unit can be set.

The time to complete the trip circuit depends upon the direction of the load current before the fault appears. If the load current is in the trip direction, the contacts of the directional unit will be closed and the time to complete the trip circuit is the time to close the contacts of the overcurrent unit. If the load current is in the nontripping direction before the fault, the contacts of the directional unit will be open, and the time required to complete the trip circuit will be the time to close the contacts of the directional unit plus the time to close the contacts of the overcurrent unit. Figure 7 shows the time to close the contacts of the overcurrent unit at various multiples of minimum pickup current. Figure 7 also shows the time to close the contacts of the directional unit when the potential circuit voltage drops to one per cent of rated voltage and the current increases to various multiples of minimum pickup current of the overcurrent unit.

Summary

The relay will operate correctly on parallel lines or on a loop circuit, regardless of whether or not there is a possibility of a reversal of current during a fault or immediately following the interruption of the fault.

The directional unit will operate correctly and quickly when a fault appears even if the potential circuit voltage drops to as low as one per cent of rated voltage.

The overcurrent unit will operate only when the contacts of the directional unit are closed.

References

1. A Single-Element Polyphase Directional Relay, A. J. McConnell. AIEE Transactions, volume 56, 1937, January section, pages 77–80.

2. Introduction to Electric Transients (book), Kurtz, Corcoran. Page 181.

The Automatic Welding-Machine Starter and Its Relation to Maximum Utilization of Power and Facilities

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MANY electric-arc-welding applications are of such a nature that the welding machine is running with no load for a large percentage of the operating time. With this low utilization factor or "arc time," no-load power consumption becomes an appreciable item. In plants employing many machines there will be long leads between the machines and the work, either by necessity or for convenience. Under these conditions, the practicability of expecting the operator, or welder, to turn his machine on and off each time he plans to weld is certainly very remote. The automatic welding-machine starter has been developed to shut down the machine during the normal no-load running periods. Automatic starters have been in operation under average shipyard welding conditions for the past few months. Application of the automatic starters effects a very appreciable saving in power cost, about 25 per cent or more. In addition, a marked reduction in average demand on distribution systems is realized so that additional machines, up to 75 per cent, may be added without further power facilities expenditure.

The Apparatus and Its Operation

The automatic welding-machine starter is an electrical relay-control mechanism which may be connected to nearly any conventional single-operator a-c to d-c motor generator welding set. Operation of the unit is simple. When ready to strike an arc, the welder firmly touches his electrode, or rod, to the article to be welded, or work, and the welding machine starts. The machine continues to run as long as welding is in progress. When welding is finished, and after a predetermined delay period of no-load operation, the machine automatically stops. The cycle may then be repeated as often as is desired.

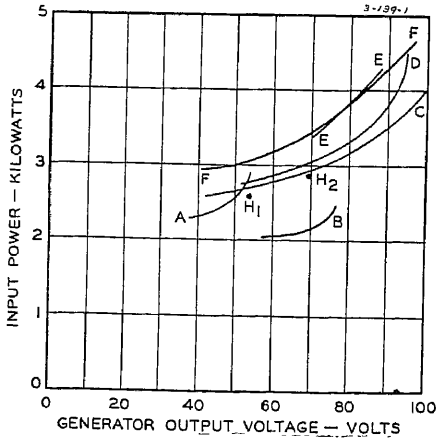

Figure 1. Welding-machine input power at no load

There are many methods of accomplishing the desired result from an electrical control standpoint. The apparatus must include the necessary relay and control components so that, when the electrode is touched to the work piece, that is, return or ground lead, thereby effecting an electrical contact, the machine will start and lock into the running position by the conventional line starter. Of particular interest is the time-delay relay as the no-load delay period directly effects power saving. A two- to three-minute interval is recommended to allow time for the welding machine to cool by ventilation. Moreover, according to welders themselves, two minutes should be allowed for changing rods and cleaning slag from the weld puddle. This prevents an unnecessary stop. Very short delay periods, such as one half a minute, increase the number of starts to a point where wear and tear on contacts is excessive. The automatic starter must be capable of immediately restarting, even at the instant the time-delay unit has called for stopping the welding machine. Any appreciable delay or erratic operation assignable to the electrical operation of the unit will have a detrimental psychological effect on the welder.

The numerous problems encountered in the development of a practical starter are mentioned but not analyzed here. Adaptability of the unit to various types and makes of welding machines, impedance of welding cable on steel plate, various contact resistances, the standstill impedance of welding generators,* and the requirements of various relays and component parts are matters of interest to the designer. A paper discussing these problems with circuit data will be prepared in the near future.

Relation to Power Cost

The no-load input for a group of popular welding machines is given in Figure 1. Nominal ratings are up to 400 amperes. Figure 2 is a plot of no-load power factor readings for the same group. The variation in input and power factor with output setting is expected and is a function of output voltage. Curves *C* and *D* represent machines with a wide range of open-circuit voltage. Less variation would be expected with machines of the more or less constant open-circuit voltage type, see curves *B* and *H*.

Power saving through the application of automatic starters is effected by many variables: the type of machine, type of welding,** output setting of the machine, the time-delay relay setting, and the cost of power.

Various power-saving tests were conducted. In all cases arc time was recorded. Arc time was measured with a synchronous time meter and relay control so that time was counted only when the arc was on. Power was measured with a graphic wattmeter for individual runs and by a watt-hour meter for banks of units. Graphic records provide other valuable data, such as the number of starts, duration of weld runs and off periods, and increase in power consumption with changes in output setting.

For arc times up to 35 per cent, the cumulative time of shutdown was found to vary between 6 and 14 hours on a 24-hour-day basis. The average centered about ten hours. The actual working-day period was checked from time-meter readings on welding machines without starters. This was found to be approxi-

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* The standstill impedance is the impedance measured across welding generator output terminals with generator at standstill.

** Power saving is effected by the type of welding that is, intermittency of welding, arc time, duration of weld runs, necessity for long delays because of fit-up, tacking, and so forth.

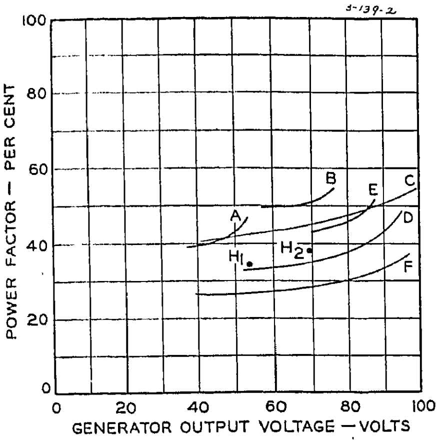

Figure 2. Welding-machine power-factor characteristics at no load

Table I. Power-Consumption Test

Test Number	1	2	3	4	5	6	7	8
Automatic starter connected during test	Yes	No	Yes	No	Yes	No	Yes	No
Test time (hours)	24	24	24	24	24	24	24	24
Power consumed by* four welding machines during test (kilowatt-hours)	277.7	284.1	199.1	378.1	305.0	462.3	315.0	497.5
Total number of arc hours of welding by four machines (hours)	20.99	15.66	19.8	23.96	26.53	26.29	23.8	27.0
Input per arc hour of welding (kilowatt-hours)	13.24	18.14	10.05	15.8	11.5	17.55	13.2	18.5
Average arc time, based on 21-hour work day (per cent)	25	18.6	23.6	28.5	30.4	31.3	28.4	32.1

Average values: Arc time..........27.3 per cent
Kilowatt-hours per arc hour *without* starters..........17.6
Kilowatt-hours per arc hour *with* starters..........12.0
Saving in power with starters..........31.7 per cent
Saving in power (kilowatt-hours per 24 hours per machine)..........31.8 kilowatt-hours

* Average no-load input 3 kw, average load input 12-kw.

mately 21 hours. The departure from a 24-hour day is accountable to shift-change and noon-hour periods. The product of the no-load losses, shutdown time, and power cost will yield dollar value of the power saving.

Actual test data of a bank of welding machines with and without starters are given in Table I. The criterion for comparison is arc time.† Power readings were taken using a three-phase watt-hour meter.

Statistics and Average Power Input

Experience and logic tell us that a power circuit supplying an intermittent load will handle a much higher connected horsepower than the same circuit under continuous operating connected load. Welding machines present a highly intermittent load.

Applying the statistical theory of sampling, we can show that, with a circuit of four welding machines operating on 25 per cent average arc time, the probability is 0.42 that we will find three machines at no load and one machine welding, the probability is 0.315 that we will find all four machines at no load, and the probability is 0.265 that there will be more than one machine welding.

It must be assumed that load conditions on the four welding machines, at any interval, represent a sample taken from a grand lot of weld (load) and no-weld (no-load) intervals,* the average arc time of which is known to be 25 per cent. Since each welder operates individually and without regard to what the others are doing, there is some justification in assuming a normal distribution of the weld and no-weld intervals. The binomial law is then applied using the formula:

$$P = \frac{n!}{(n-c)!c!}\, p^{n-c}Q^{c}$$

This is simply the probability of finding c defectives in a sample of size n, where n is the number of welding machines, c is the number not welding, P is the fraction effective or average arc time (25 per cent effective in grand lot), and $Q = 1 - P$ the average not welding or fraction defective (the average not welding in the grand lot of weld and no-weld intervals).

If we increase the sample size,** n, or number of welding machines, it can be seen that factorial calculations become laborious. Moreover, if we wish to calculate the probability that there will be not more than a given number of machines welding, it is necessary to sum up a group of terms. Calculations then make use of the incomplete beta function for which charts have been prepared.[2]

Using charts on the incomplete beta function, we can show that for 100 welding machines operating on a 25 per cent average arc time, the probability is 0.955 (extremely probable) that there will never be more than 36 machines welding. Likewise, we can show that the probability is 0.900 (highly probable) that there will be not more than 30 machines welding for a given interval. This may be expressed differently, that is, for at least 90 per cent of the time there will not be more than 30 machines welding and not less than 70 machines at no load.

The statistical analysis is interesting and gives us a fair approximation of how close we may load a distribution system to the average load limit. As the number of machines is increased, the statistical analysis is more accurate, and average load values may more safely be used.

Relation to Maximum Utilization of Facilities

The relation of automatic starters to maximum utilization of facilities can best be emphasized through a hypothetical example, making use of certain reasonable assumptions. Consider a distribution system operating at capacity and supplying 100 welding machines at an average arc time of 25 per cent.‡ Average no-load input is three kilowatts and 40 per cent power factor. (See Figures 1 and 2.) From test data, the average welding input is 12 kw and 85 per cent power factor. If no automatic starters are employed, the average input will be:

$0.25 \times 100 \times 12$ kw $= 300$ kw at 85 per cent power factor, 187 rkva, 355 kva
$0.75 \times 100 \times 3$ kw $= 225$ kw at 40 per cent power factor, 516 rkva, 562 kva
Average input = 525 kw, 877 kva, and 50.8 per cent power factor.

If the machines are operated on a 21-hour work-day period and the installation of automatic starters results with an average shutdown time of ten hours per day per machine. Converting hours to per cent time on a 25 per cent arc time basis, the average input will be:

$0.25 \times 100 \times 12$ kw $= 300.0$ kw at 85 per cent power factor, 187 rkva, 355 kva
$0.274 \times 100 \times 3$ kw $= 82.2$ kw at 40 per cent power factor, 188 rkva, 205 kva

Average input is 382.2 kw, 535 kva, and 71.5 per cent power factor. The application of starters effects a decrease 27.2 per

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† For shipyards, 25 per cent arc time is believed to be a conservative average.

* From graphic instrument charts and impulse counter readings, it is found that the number of impulses, or times that a welding arc is struck, in a 24-hour work day will be in order of 2,000. This may substantiate the concept of weld and no-weld intervals and accordingly, statistical normality.

** It is sometimes convenient to calculate factorial n when n is not too large. Stirling's formula gives satisfactory results. The formula:

$$n = n^{n}e^{-n}\sqrt{2\pi n}$$

For $n = 10$, the error is only 0.8 per cent.[1]

‡ The machines were welding, on the average, for 25 per cent of the time, or 5.25 hours on a 21-hour-work-day basis. They were off, on the average of ten hours, and running at no load on the time-delay relay period for the balance of the time, 5.75 hours, which is 27.4 per cent of the period.

cent in average kilowatt demand, 39 per cent in average kilovolt-ampere demand, 46.7 per cent in average reactive-kilovolt-ampere demand, and an increase of 11.7 per cent in average power factor. With power at $0.01 per kilowatt-hour, on a 21-hour work-day period, this amounts to approximately $900 per month. If the installed cost is $60.00 per unit, starters will pay for themselves on power savings alone in less than seven months. The average reactive demand is reduced by 328 reactive kilovolt-amperes. In equivalent capacitance correction, at $10.00 per kilovolt-ampere installed, this represents more than half the first cost of the starters.

In the example, application of automatic starters allows for an increase of 64 per cent in capacity or 64 additional welding machines on the already loaded distribution system. Higher arc times will increase the average input and reduce the additional capacity allowance, and vice versa. Power saving and investment value will depend upon power rates and schedules. The saving will increase where power is sold on some form of kilovolt-ampere-hour basis.

Suppose in this hypothetical case, it was decided to purchase 60 additional welding machines at a cost of $450.00 each. Since the distribution system is operating at capacity, power supply for the new machines becomes a serious problem. There are three solutions to the problem:

1. Installation of additional distribution capacity.

2. Power factor correction of the existing system.

3. Application of automatic welding machine starters.

It may be desirable to write off the expenditure in a short period such as six months.

Using a typical rate, power cost is $0.007 per kilowatt-hour, and the total charge per month is decreased or increased, respectively, by 0.25 per cent for each one per cent average power factor above or below 85 per cent. Estimates, neglecting interest, depreciation, and items common to all methods, are given in Table II.

Table II

Item	Method 1 Additional Capacity	Method 2 Power-factor Correction	Method 3 Starters
1. Facilities cost	$ 7,900.00 (a)	$ 8,730.00 (b)	$ 9,600.00 (c)
2. Power cost, per month for 160 machines	3,933.00	3,601.00	2,775.00
3. Original power cost per month for 100 machines	2,460.00	2,460.00	2,460.00
4. Additional power cost per month for operating 60 machines (item 2 minus 3)	1,473 00	1,141.00	315.00
5. Additional power cost for 6 months period	8,850.00	6,846.00	1,890.00
6. Cost of facilities and power for 6 months (item 1 plus 5)	16,750.00	15,576.00	11,490.00
7. Cost per machine per month for 6 months period (not including machine cost)	279.00	260.00	191.00
8. Per cent of unit machine cost @ $450.00 per unit	62 per cent	27.7 per cent	42.5 per cent
9. Continued power saving per month over method 1	None	332 00	1,158 00

(*a*). 550 kva of additional capacity at $15.00 per kilovolt-ampere installed.

(*b*). 873 kva of correction required at $10.00 per kilovolt-ampere installed.

(*c*). 160 starters at $60.00 each installed. No allowance is made for maintenance in method 3. This item must ever be recognized where moving parts, that is, relays, and so forth, are employed. It is believed, however, that the maintenance cost for starters would be at least offset by the decreased maintenance cost on the welding machines themselves, because, on account of the starters, their total running time will be greatly reduced.

A continued power saving of $332.00 per month by method 2 and $1,158.00 per month by method 3, over method 1, will be realized. Longer investment periods will naturally place the balance more and more in favor of the automatic welding-machine starters. It is believed that the figures used and shorter period are in keeping with many wartime expansion policies. It is of interest to note that a combination of capacitance correction and starters would allow for more than doubling the capacity of the original loaded system in the example.

Conclusions

The automatic welding-machine starter has been in operation under average shipyard welding conditions for a sufficient length of time to prove its practicability and merits. Application of automatic starters offers two principal advantages:

1. A very recognizable saving in power cost is effected because the welding machine is automatically shut down for the greater part of its normal no-load operating time. Test results show this saving to be in order of 25 to 30 per cent of the total power consumed for average arc times of 25 per cent and with a three-minute delay setting on the automatic starter. The saving will vary with different welding application and will increase as the average arc time is reduced and with shorter time-delay relay settings. Since the no-load input for a-c to d-c motor generator welding sets is inherently of a low power factor nature, application of automatic starters greatly reduces the average reactive-kilovolt-ampere demand (in the order of 50 per cent reduction), so that the average kilovolt-ampere input is reduced far more than the average kilowatt input The economic advantage of automatic starters on a purely power cost saving analysis is entirely dependent upon power rate schedules and will be more favorable where power is sold on some form of kilovolt-ampere-hour basis.

2. Probably the most important advantage offered by the application of automatic starters is that, by reducing the average kilowatt and kilovolt-ampere demand, appreciable increase in capacity on existing distribution systems may be realized. The increased capacity allowance will vary from 25 per cent to 75 per cent, depending upon arc time and other conditions, and may be over 100 per cent in cases where starters are used in combination with capacitance correction.

It is believed that the application of starters will warrant consideration where new or additional distribution facilities are contemplated. In general, starters will show a greater economic advantage over any other method of reducing overall power and facilities cost and, in any case, will provide for the maximum utilization of welding power distribution systems.

References

1. Higher Mathematics for Engineers and Physicists (book), **Sokolnikoff and Sokolnikoff.**

2. The Engineers' Manual of Statistical Methods, **Leslie E. Simon.**

Lightning Surges Transferred From One Circuit to Another Through Transformers

P. L. BELLASCHI
FELLOW AIEE

IT is generally recognized that through the medium of transformers surges can be transferred from circuits exposed to lightning to circuits considered otherwise nonexposed. The latter may connect to rotating machines or to industrial apparatus of inherently low-impulse insulation level. On these seemingly nonexposed circuits may be found apparatus of old design which as a rule are even more vulnerable to surges. In these problems the question is to determine

1. The extent apparatus may be endangered.
2. Whether the relative risk incurred weighed against the importance of the installation and the cost of damage, warrants protective equipment.

In each case sound engineering recommendations demand a fairly close estimate of the transferred surges.

In examining cases of this character in addition to published data, the writer has found particularly helpful the results of an investigation at Sharon in 1932–33. Seasoned by experience, these results and the simplified methods derived from them are presented here to assist in a practical way those engaged in this type of protection studies. This contribution should serve besides to call attention again to the vital importance transferred surges may assume under certain conditions.

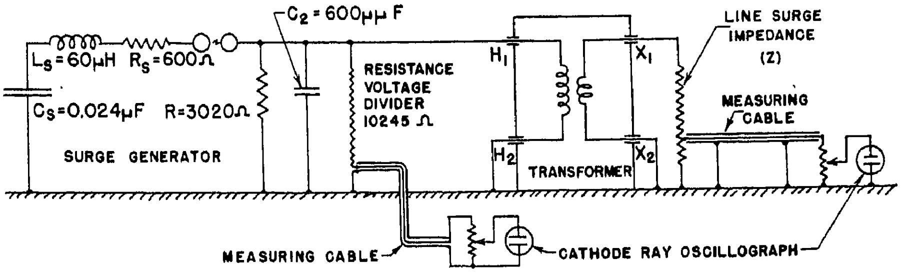
The Nature of the Problem

Contributions on the mechanism governing the transfer of surges through transformers have been reported by various investigators.[1–5] Concurrently, the protection of rotating machines and similar low-impulse-strength apparatus subject to these surges has been discussed.[6–11] Essentially, when a lightning surge is impressed on one winding of a transformer, a surge appears at the terminals of a second winding. The surge thus transferred to other windings and connected circuits consists respectively of an electrostatic component and an electromagnetic component. The electrostatic component results from the capacitance coupling between the windings and to ground (Figure 13), while the electromagnetic component depends on the turn ratio and the short-circuit inductance between the two windings (Figure 15). The terminal conditions naturally contribute also to determine the amplitude, the wave form, and the duration of the surge transferred. Superimposed on these components will be present in lesser or greater amounts the effects due to oscillations from the windings. All these factors will be apparent from the tests and the discussion which follow.

One Series of Tests

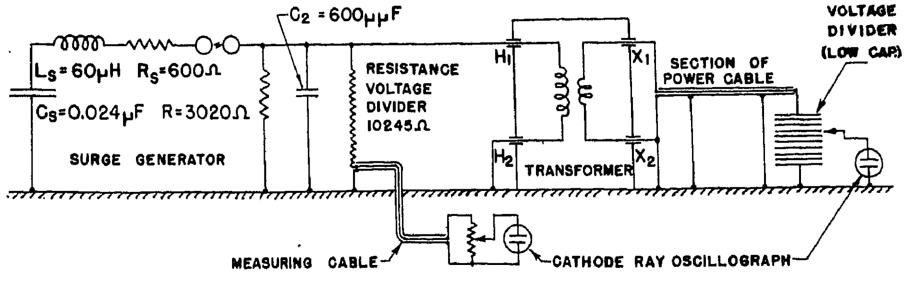
All tests and the corresponding measurements were conducted in the manner illustrated in Figure 1. In this series of tests the transformer is a single-phase two-winding core-form unit, rated at 1,000 kva and 76,210/2,300 volts. The turn ratio is 33.1 to 1. Other characteristics of the transformer are listed in Table I. The terminal arrangements cover a wide range of the practical conditions encountered in service. These are summarized in Figure 2. The voltage applied

Figure 1. Typical arrangements and method of test



A (above). Low-voltage terminal of transformer connected to line

B (below). Low-voltage terminal of transformer connected to section of cable



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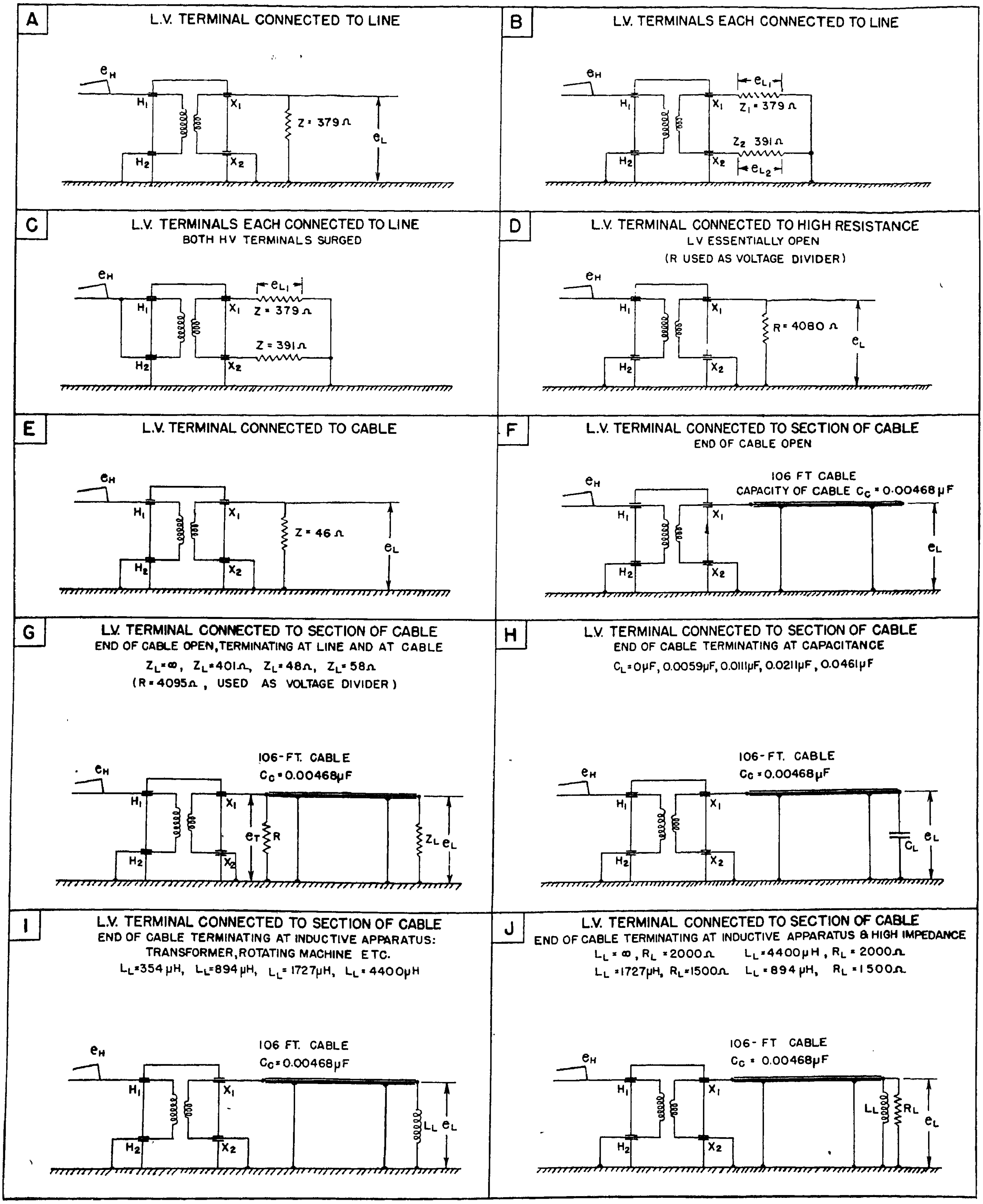

A
L.V. TERMINAL CONNECTED TO LINE
eH
H1
H2
X1
X2
Z = 379Ω
eL
B
L.V. TERMINALS EACH CONNECTED TO LINE
eH
eL1
Z1 = 379 Ω
Z2 391 Ω
eL2
H1
H2
X1
X2
C
L.V. TERMINALS EACH CONNECTED TO LINE
BOTH HV TERMINALS SURGED
eH
eL1
Z = 379 Ω
Z = 391 Ω
H1
H2
X1
X2
D
L.V. TERMINAL CONNECTED TO HIGH RESISTANCE
LV ESSENTIALLY OPEN
(R USED AS VOLTAGE DIVIDER)
eH
R = 4080 Ω
eL
H1
H2
X1
X2
E
L.V. TERMINAL CONNECTED TO CABLE
eH
Z = 46 Ω
eL
H1
H2
X1
X2
F
L.V. TERMINAL CONNECTED TO SECTION OF CABLE
END OF CABLE OPEN
106 FT CABLE
CAPACITY OF CABLE Cc = 0.00468 μF
eH
eL
H1
H2
X1
X2
G
L.V. TERMINAL CONNECTED TO SECTION OF CABLE
END OF CABLE OPEN, TERMINATING AT LINE AND AT CABLE
ZL = ∞, ZL = 401Ω, ZL = 48Ω, ZL = 58Ω
(R = 4095Ω, USED AS VOLTAGE DIVIDER)
106-FT. CABLE
Cc = 0.00468μF
eH
eT
R
ZL
eL
H1
H2
X1
X2
H
L.V. TERMINAL CONNECTED TO SECTION OF CABLE
END OF CABLE TERMINATING AT CAPACITANCE
CL = 0μF, 0.0059μF, 0.0111μF, 0.0211μF, 0.0461μF
106-FT. CABLE
Cc = 0.00468μF
eH
CL
eL
H1
H2
X1
X2
I
L.V. TERMINAL CONNECTED TO SECTION OF CABLE
END OF CABLE TERMINATING AT INDUCTIVE APPARATUS:
TRANSFORMER, ROTATING MACHINE ETC.
LL = 354 μH, LL = 894 μH, LL = 1727μH, LL = 4400μH
106 FT. CABLE
Cc = 0.00468μF
eH
LL
eL
H1
H2
X1
X2
J
L.V. TERMINAL CONNECTED TO SECTION OF CABLE
END OF CABLE TERMINATING AT INDUCTIVE APPARATUS & HIGH IMPEDANCE
LL = ∞, RL = 2000Ω   LL = 4400μH, RL = 2000Ω
LL = 1727μH, RL = 1500Ω   LL = 894 μH, RL = 1500Ω
106-FT CABLE
Cc = 0.00468μF
eH
LL
RL
eL
H1
H2
X1
X2

Figure 2. Terminal conditions for tests with 1,000-kva 76,210/2,300-volt single-phase core-form transformer

to the high-voltage winding (e_H) measures 364-kv crest, and in this too the tests have the significance of approaching the conditions of interest in actual service. Likewise, the surge applied corresponds quite well to the standard $1^1/_2$x40-microsecond wave.

For test condition *A*, one low-voltage terminal connects to an impedance $Z=$ 379 ohms, the equivalent of a long overhead line. The other terminal goes directly to ground. The surge voltage e_L transferred to the low-voltage terminal and to the line is recorded in Figure 3. In effect it rises to crest in eight microseconds, and recedes on the tail in about the same time as the wave applied. The

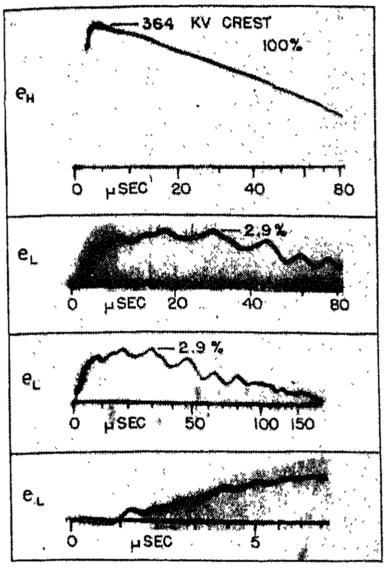


Figure 3. Oscillograms for test condition *A* (Figure 2)

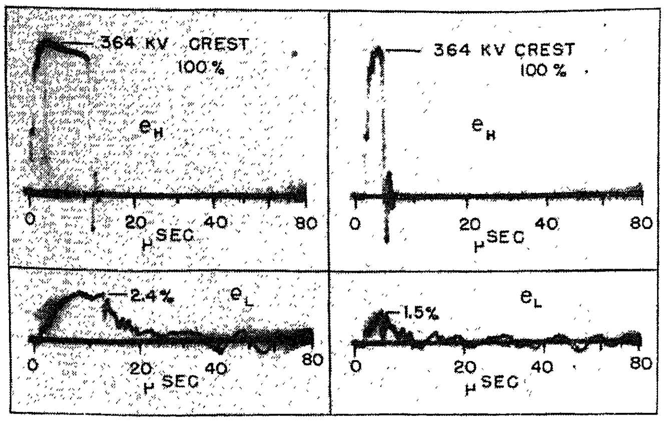


Figure 4. Oscillograms for test condition *A* (Figure 2)

electrostatic component accounts in part for the front. Beyond crest the electromagnetic component plus the superimposed oscillations predominate. The crest value 2.9 per cent corresponds essentially to turn ratio (100 per cent divided by 33.1). Test *A* was repeated with chopped waves applied. From Figure 4, as it should be expected, the surge e_L transferred to the line practically disappears for increasingly shorter impulses e_H.

In test *B*, each of the low-voltage terminals connects to a line impedance. In this case, the electromagnetic component divides about equally between the two lines, as in e_{L1} and e_{L2} of Figure 5. Note also the opposite polarity with respect to ground of the two. Only on the low-voltage terminal, physically opposite H_1, does the electrostatic component occur. In test *C*, surge e_H is applied simultaneously to both high-voltage terminals, so that in this case the electromagnetic component in e_L (Figure 5), disappears. Here the electrostatic component (1.3 per cent) is followed by the natural oscillations set up in the winding. In test *D*, the high resistance practically amounts to an open low-voltage terminal. Under this condition, because of the full development of the electrostatic effect and similarly because of the rapid development of the electromagnetic effect, the surge e_L (Figure 5) rises more abruptly and reaches a higher crest (3.7 per cent) than in any of the previous tests. The bottom oscillogram e_L of Figure 5 shows the influence of a long cable. This condition corresponds to *E*. As the result of the low impedance of the cable, the electrostatic effect practically has vanished insofar as appearing at the terminal. The surge transferred to the cable rises to crest in a much longer time than for the overhead line because of the greater time constant L_S/Z, attaining 2.1 per cent crest as against 2.9 per cent for the line.

In service transformer terminals may connect to a section of cable which, under certain conditions, may remain open in the manner of test *F*. In this case, as recorded in Figure 6, the cable voltage oscillates, reaching 5.2 per cent, which is nearly twice the voltage e_H divided by the turn ratio. The oscillations are largely between the short-circuit inductance (L_S) and the cable capacitance (C_C), though in part other winding effects enter into play. A chopped wave e_H results in a transferred surge of the character shown.

Under other service conditions, the section of cable may connect directly to an overhead line or continue with another cable for a great extension. The voltages developed under these conditions (test *G*) are shown in Figure 7. For either the long line or extensive cables the section of cable is not a major influencing factor. The oscillograms show that the voltages at the two ends of the cable section are much the same.

In test *H*, capacitances were connected to the open end of the cable. The capacitances applied in effect are equivalent to extending the 106-foot cable respectively to 240, 360, 580, and 1,120 feet. The results are recorded in Figure 8. The period of the transferred surge naturally increases with the capacitance, but the amplitude remains practically unchanged at 5.1 per cent.

Another practical case occurs where the transformer supplies inductive apparatus such as other transformers or rotating machines. This condition is approached in test *I*. On the basis that the short-circuit inductance (L_S) of the transformer is 100 per cent, the inductances L_L applied are respectively 26, 66, 128, and 325 per cent which values correspond to a wide range of effective loads. The oscillograms of Figure 9 show that both the amplitude and the period of the trans-

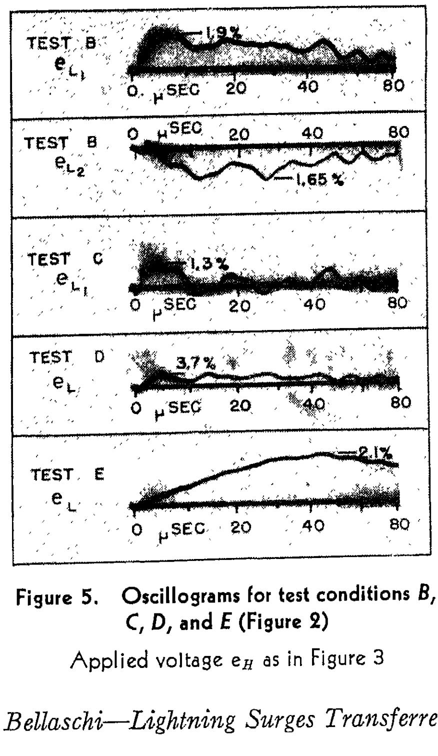


Figure 5. Oscillograms for test conditions *B*, *C*, *D*, and *E* (Figure 2)

Applied voltage e_H as in Figure 3

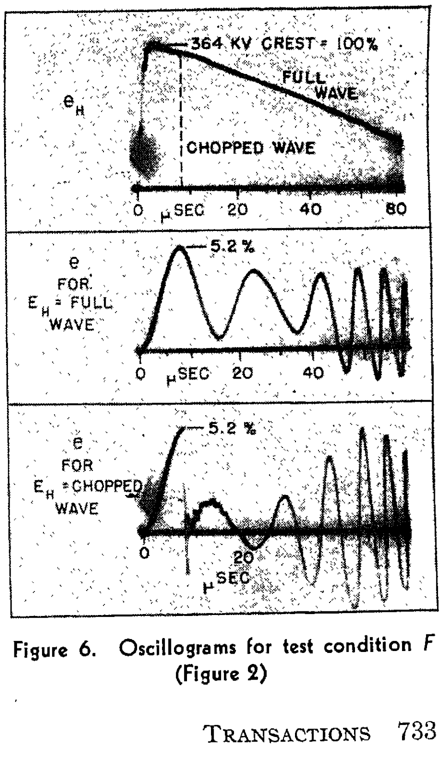


Figure 6. Oscillograms for test condition *F* (Figure 2)

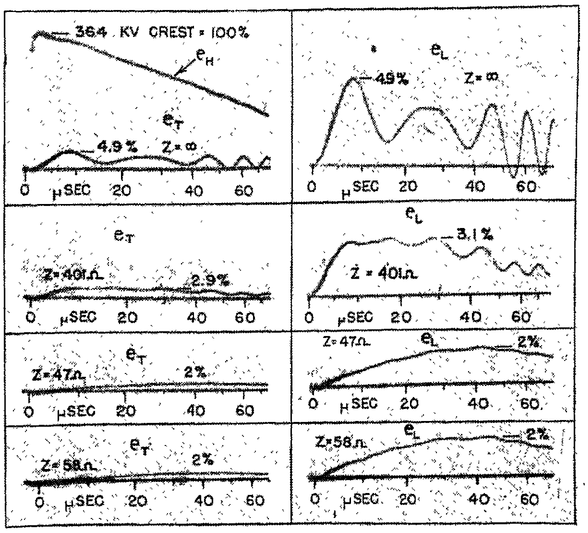


Figure 7. Oscillograms for test condition *G* (Figure 2)

ferred surge decrease with decreasing inductance load.

Another set of load conditions is the combination of high surge impedances (resistances) and inductances in *J*. The corresponding transferred surges are recorded in Figure 10. These tests complete the investigation on the 1,000-kva transformer.

Other Tests and Experiences

In Figure 11 are recorded the voltages transferred through a single-phase 4,500-kva 144,000/12,000-volt, shell-form transformer. The turn ratio is 12 to 1.

Table I. Design Characteristics of Transformers Used in Tests

1. Core-form, oil-immersed, self-cooled, 1,000-kva, 76,210/2,300-volt, single-phase, 60-cycle
   - Number of turns: high-voltage, 3,576 turns
     low-voltage, 108 turns
   - Turn ratio = 33.1
   - Impedance = 9.62 per cent
   - Short-circuit inductance (referred to low voltage) = 0.00135 henry
   - Short-circuit inductance (referred to high voltage) = 1.48 henrys
   - Construction, two-legged design
   - High-voltage winding, pancake coils, one column each leg
   - Low-voltage winding, two layers on each leg adjacent to core
2. Shell-form, oil-immersed, self-cooled, 4,500-kva, 144,000-120,000/12,000-volt, single-phase, 25-cycle
   - Number of turns: high-voltage, 1,582 turns
     low-voltage, 132–158 turns (taps)
   - Turn ratio (in tests) = 12
   - Impedance = 4 per cent
   - Short-circuit inductance (referred to low voltage) = 0.008 henry
   - Short-circuit inductance (referred to high voltage) = 1.15 henrys
   - Construction, four high–low interleaved (see Figure 13A)
   - Two high-voltage groups each 791 turns
   - Low-voltage center group 62–88 turns (taps)
   - Two low-voltage end groups each 35 turns
3. Shell-form model, oil-immersed, special
   - No rating assigned
   - Turn ratio = 6.14
   - Construction (see Figure 12)
   - High-voltage coil group 2,180 turns
   - Low-voltage coil *A* 18 turns
   - Low-voltage coil *B* 337 turns

Other characteristics of this transformer are listed in Table I. In these tests the low-voltage winding in effect connects to a long overhead line, while the impulse voltages applied to the high-voltage winding reach practically the full test level of the insulation. The electrostatic component is clearly in evidence for the first ten microseconds; beyond this time the electromagnetic component with some superimposed oscillations of minor import is the dominating factor. In the chopped-wave test, the electromagnetic component practically disappears. The tests were repeated with a 100-foot section of cable connected to the low voltage as in *B* of Figure 1. In these tests the crest voltage developed was sufficient to flash over the cable at the end.

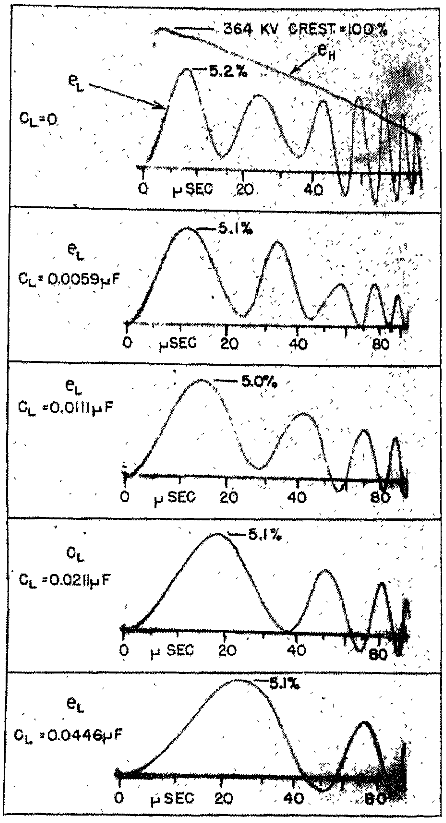


Figure 8. Oscillograms for test condition *H* (Figure 2)

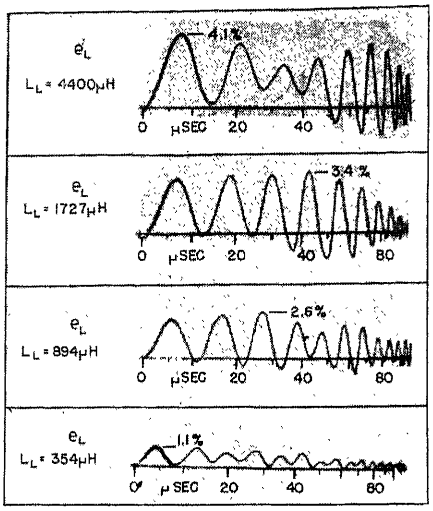


Figure 9. Oscillograms for test condition *I* (Figure 2)

Applied voltage e_H as in Figure 3

In the course of the many commercial impulse tests applied to transformers at Sharon, the effects of surges transferred through windings have been much in evidence. Bushings of windings not under test often require protection from flashover. In this connection, it is well to point out that unusually high surge voltages can be transferred to high-voltage terminals when low-voltage windings of high insulation level are being tested. For instance, a 110-kv full-wave surge applied to the low-voltage windings of a 6,600/132,000-volt power transformer (ratio 20 to 1) is certain to flash over the high-voltage bushing.

The experience from commercial im-

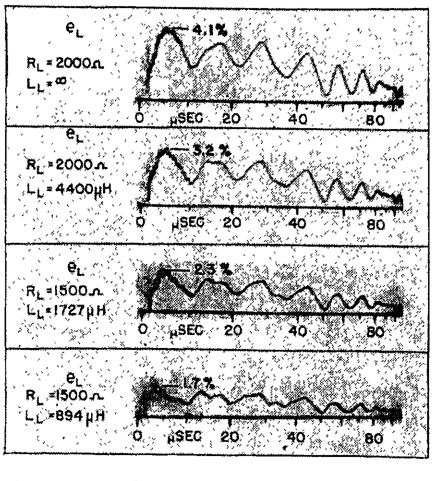


Figure 10. Oscillograms for test condition *J* (Figure 2)

Applied voltage e_H as in Figure 3

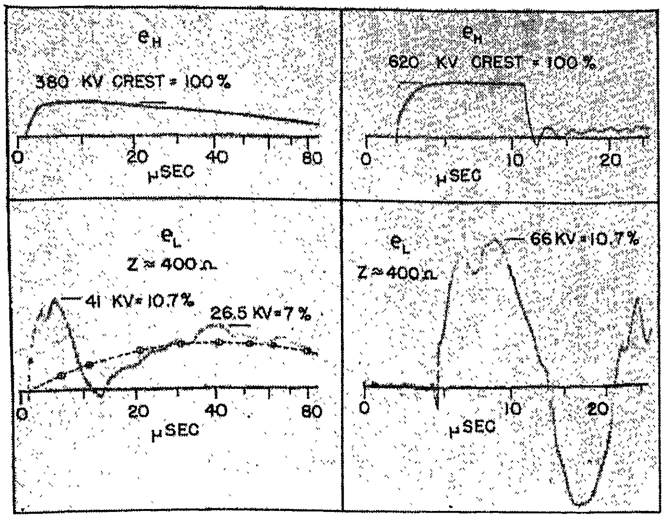


Figure 11. Oscillograms for test condition *A* (Figure 1) with 4,500-kva 144,000/12,000-volt single-phase shell-form transformer

Circled dots are calculated values of electromagnetic component

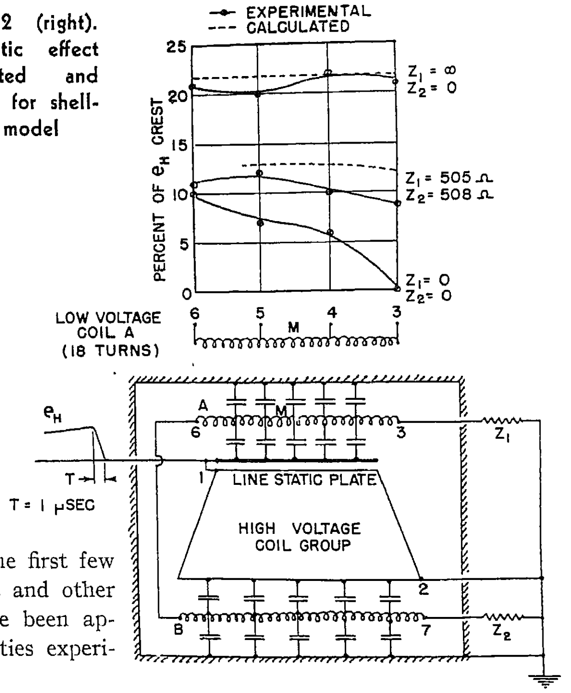


Figure 12 (right). Electrostatic effect—calculated and test data for shell-form model

pulse tests at Sharon bears out also that surges may be transferred through a number of transformers in succession. In the commercial tests, power excitation is supplied from a 5,000-kva, 2,300-volt generator of old design through a 2,000-kva 2,300/6,900-volt transformer, a 1,000-foot cable, and again through another 2,000-kva transformer which serves to adjust the 6,900-volt cable supply to the primary voltage of the transformer under impulse test. Depending on the nature of the commercial impulse test, transferred surges of various intensities would reach the rotating machine. These seriously damaged the machine windings on three occasions in the course of the first few years. Suitable capacitances and other protective devices have since been applied and no further difficulties experienced.

Electrostatic Component and Oscillations

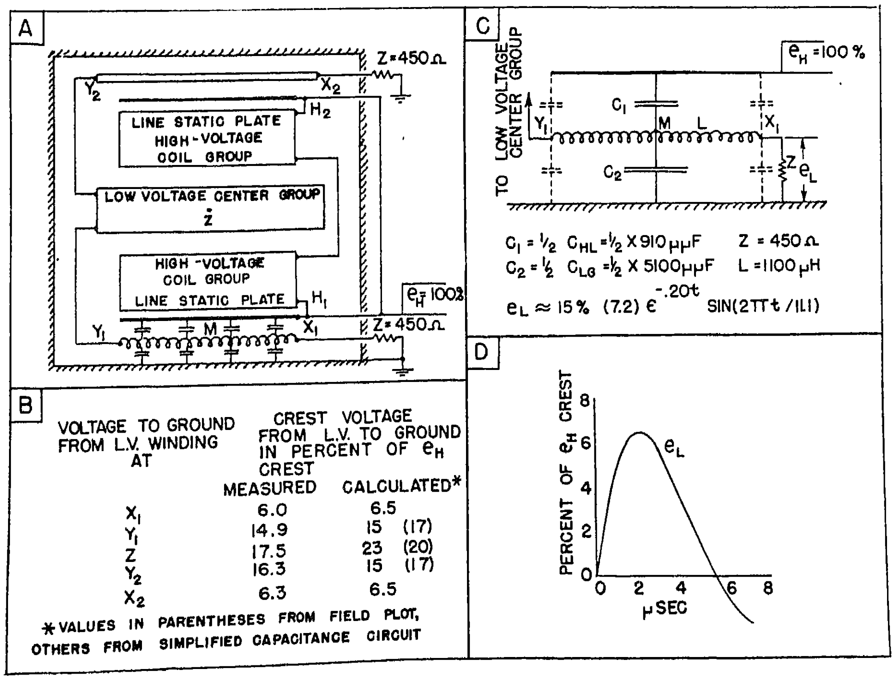
The factors which affect the electrostatic component are several. These are discussed here in simple practical terms with reference to the typical examples in Figures 12 and 13. The electrostatic voltage induced in the low-voltage winding depends upon the capacitance relation between the high and low-voltage windings and to ground. In turn, the voltage induced in the low-voltage winding is discharged through the inductance of part of the winding into the terminal impedance. Thus from the simplified circuit in *C* of Figure 13 for the abrupt surge e_H,

$$e_L \approx \frac{C_1}{C_1+C_2}\cdot\frac{Z\epsilon^{-\frac{Z}{2L}t}\sin t\sqrt{\dfrac{1}{LC_2}-\dfrac{Z^2}{4L^2}}}{\sqrt{\dfrac{L}{C_2}-\dfrac{Z^2}{4}}}\,e_H \tag{1}$$

The calculated values from this simplified approach are in fairly good agreement with the test results. A more rigorous analysis requires that account be taken of the additional circuit elements shown in Figures 12 and 13 but in so doing the solution becomes quite complex.

Simply stated, the electrostatic component depends on the ratio, Z divided by $\sqrt{L/C_2}$, and on the length of the front of surge e_H with respect to the natural period $2\pi\sqrt{LC_2}$. In the case of a long cable where Z is small or for a segment of cable of large capacitance, the electrostatic component disappears. The electrostatic component needs be considered only where the low-voltage winding connects to aerial lines, to highly inductive apparatus or machinery, to loads of effectively high impedance, and where the capacitance to ground is small. The physical characteristics of the transformer windings are a factor, but these do not bear any simple relation in terms of the usual design constants. The ratio of the insulation test levels of the windings is a measure of the factor $C_1/(C_1+C_2)$ and, other things being equal, of the relative

Figure 13. Electrostatic effect—calculated and test data for 4,500-kva shell-form transformer



$C_1 = \tfrac{1}{2}\ C_{HL} = \tfrac{1}{2} \times 910\ \mu\mu F$ $Z = 450\ \Omega$

$C_2 = \tfrac{1}{2}\ C_{LG} = \tfrac{1}{2} \times 5100\ \mu\mu F$ $L = 1100\ \mu H$

$e_L \approx 15\%\ (7.2)\ \epsilon^{-.20t}\ \sin(2\pi t/1.11)$

VOLTAGE TO GROUND FROM L.V. WINDING AT	CREST VOLTAGE FROM L.V. TO GROUND IN PERCENT OF e_H CREST	
	MEASURED	CALCULATED*
X_1	6.0	6.5
Y_1	14.9	15 (17)
Z	17.5	23 (20)
Y_2	16.3	15 (17)
X_2	6.3	6.5

*VALUES IN PARENTHESES FROM FIELD PLOT, OTHERS FROM SIMPLIFIED CAPACITANCE CIRCUIT

Table II. Electromagnetic Component—Comparison of Calculated Data With Tests for 1000-Kva Core-Form Transformer

Test Condition	Terminal Impedance	Crest Voltage eL in Per Cent eH Crest Calculated*	 Experimental	Wave Form
Figure 2A	$Z_L=379$ ohms	2.6	2.9	See Figure 14 for comparison of calculated* and recorded wave form
Figure 2E	$Z_L=$ 46 ohms	1.6	2.1	
Figure 2F	$C_C=0.00468$ microfarad	5.8	5.2	
Figure 2H	$C_C+C_L=0.0493$ microfarad	5.2	5.1	

* Data calculated from equations 2 and 3, transformer constants in Table I, and terminal impedances in column 2 above. Wave form of e_H in calculations approximately as in tests.

amplitude of the electrostatic component. The calculations and tests in Figures 12 and 13 and in the preceding oscillograms are quite indicative of the amplitude and duration of the electrostatic components expected in general.

As it may be seen from the oscillograms, the electrostatic impact sets the low-voltage winding oscillating. In addition oscillations in the high-voltage winding are induced electromagnetically in the low-voltage winding, and these in turn appear in the transferred surge. Simplified calculations of these effects are beset with difficulties. In general, however, the superimposed oscillations are not of primary importance.

Table III. Comparison of Period of Oscillation for Various Cable Capacitances

Short-Circuit Inductance (L_S in Microhenrys)	Cable Capacitance (C_O in Microfarads)	Period of Oscillation Calculated ($T=2\pi\sqrt{L_SC_O}$) (Microseconds)	 Experimental (Figure 8) (Microseconds)
1,350	0.00468	15.8	16
1,350	0.0106	24.8	23
1,350	0.0158	29.0	28
1,350	0.0258	37.0	38
1,350	0.0493	51.3	52

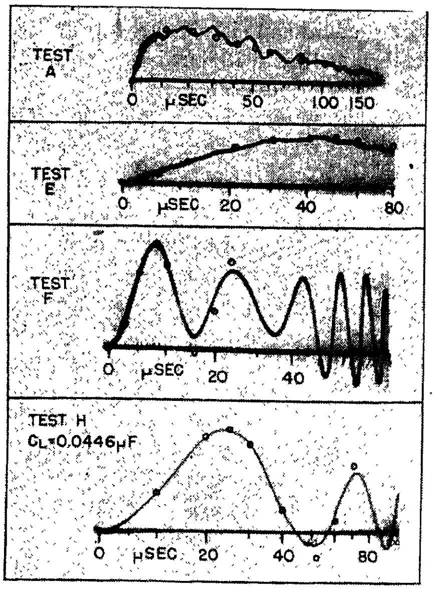


Figure 14. Electromagnetic effect—calculated (circled dots) and test waves of $_L$ for 1,000-kva core-form transformer

Test conditions *A*, *E*, *F*, and *H* (Figure 2)

Electromagnetic Effect

SURGE TRANSFERRED TO A LONG LINE OR CABLE

The general solution for the electromagnetic component is treated in the appendix. From a practical standpoint, refer to the equivalent simplified circuit of Figure 15D and let

$e_H=E\epsilon^{-\alpha t}$ = impulse voltage applied to high-voltage winding

L_S = short-circuit inductance of transformer (referred to low-voltage side)

N = turn ratio

Z = surge impedance of line or cable

Then the electromagnetic component transferred to the line or cable is

$$e_L=\frac{Z}{Z-\alpha L_S}\left[\epsilon^{-\alpha t}-\epsilon^{-\frac{Z}{L_S}t}\right]\frac{E}{N} \qquad (2)$$

In Figures 11 and 14 and in Table II are compared the calculations for three typical cases with the corresponding experimental data. It is apparent that for these conditions the electromagnetic component is conducive to fairly good predetermination. The preceding equation can be adapted readily to similar conditions such as those shown in tests *B*, *C*, and *D* of Figure 2. The behavior for shorter lengths of cable and line is considered in the following section.

SURGE TRANSFERRED TO SECTION OF CABLE (OR LINE)

For this condition the simplified circuit of Figure 15E applies. Let the applied voltage $e_H=E\epsilon^{-\alpha t}$, then

$$e_L=\left[\frac{\epsilon^{-\alpha t}}{1+\left(\frac{\alpha}{\omega}\right)^2}+\frac{\sin\left(\omega t-\tan^{-1}\frac{\omega}{\alpha}\right)}{\sqrt{1+\left(\frac{\alpha}{\omega}\right)^2}}\right]\frac{E}{N} \qquad (3)$$

where $\omega=1/\sqrt{L_SC_C}$ and C_C = capacitance of cable section.

On the whole, the comparison between the calculated and experimental data in Figure 14 and in Table II for the 106-foot section of cable is good. Equation 3, however, does not take into account the attenuation due to losses, nor does it consider the capacitance of the windings and other effects which apparently are responsible for the "beat-note" phenomena present in the oscillations.

As shown in the oscillograms of Figure 7, the cable section terminating in a lumped impedance equal to the surge impedance of the cable behaves essentially as a continuous long cable. In the case where the cable section connects to a long overhead line, the cable voltage no longer is oscillatory. The reason for this lies in the fact that the line surge impedance ($Z=401$ ohms) appears in Figure 15E as a circuit element shunting the cable capacitance ($C_C=0.00468$ microfarad), and its value approaches closely the

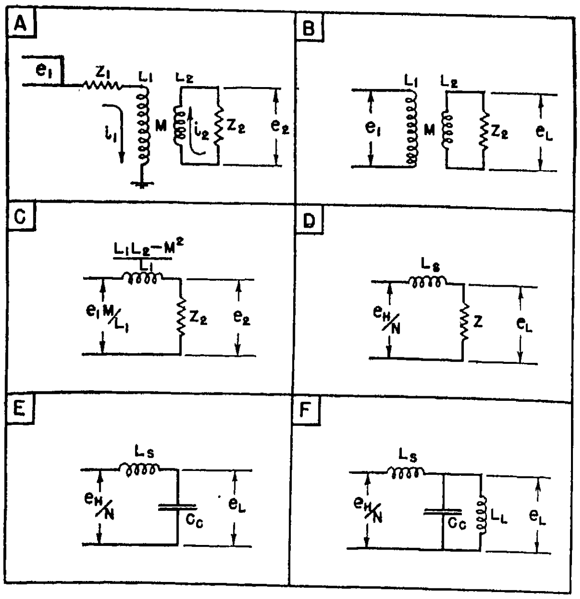


Figure 15. Typical circuits for surges transferred through windings

Simplified circuits *D*, *E*, and *F* represent low-voltage terminal connected, respectively, to long line or cable (*Z*), to cable section (*C*$_C$) and to cable section (*C*$_C$) terminating at induction apparatus (*L*$_L$)

critical damping impedance $^1/_2\sqrt{L_S/C_C}=$ 270 ohms.

In test H of Figure 2, the load capacitances amount in effect to extending the cable section. The surge transferred to these sections is given by equation 3 where the cable capacitance in the equation now becomes $C_O=C_C+C_L$. In Table III are compared the calculated and experimental periods of oscillation for the various cable capacitances.

In Figure 14 and Table II are compared the calculated and experimental data of the transferred voltage for $C_L=0.0446$ microfarad. This load capacitance is equivalent to extending the cable to 1,120 feet. In an actual section of this length, the cable would have an inductance less than ten per cent of L_S. For a much longer section, it is apparent that, besides the capacitance, the inductance of the cable need be considered. That is, in Figure 15E for segments of cable where the total inductance $L_C=Z^2C_C$ (constants of cable) is small relative to the transformer short-circuit inductance L_S, the cable may be treated as a lumped capacitance. For longer sections where the cable inductance becomes appreciable but is not excessively large compared to L_S, the cable may be replaced with good approximation by its equivalent π circuit. For still longer sections of cable (or line), transferred surges should be dealt with in terms of traveling-wave phenomena.

Surge Transferred to Section of Cable and Terminating Inductive Apparatus

For this condition the simplified circuit is given in Figure 15F. The capacitance C_C includes the capacitance of the connecting apparatus, or, in the absence of a cable section, it represents the capacitance of the apparatus alone. For an applied surge $e_H=E\epsilon^{-\alpha t}$, the transferred surge is

$$e_L=\left[\frac{\epsilon^{-\alpha t}}{1+\left(\frac{\alpha}{\omega}\right)^2}+\frac{\sin\left(\omega t-\tan^{-1}\frac{\omega}{\alpha}\right)}{\sqrt{1+\left(\frac{\alpha}{\omega}\right)^2}}\right]\times\frac{L_L}{L_S+L_L}\cdot\frac{E}{N}\qquad(4)$$

where L_L is the inductance of the connecting apparatus, and the period of oscillation is

$$T=\frac{2\pi}{\omega}=2\pi\sqrt{C_C\frac{L_SL_L}{L_S+L_L}}$$

In the investigation, L_L varies from approximately 25 to 325 per cent of the short-circuit inductance of the transformer (L_S), and therefore these values comprehend a wide range of inductances of the connecting apparatus (transformers, rotating machines, and so forth). In Table IV are compared the calculated and experimental data of the transferred surges appearing at the connecting apparatus. The good agreement again confirms the applicability of the simplified methods for practical calculations.

Table IV. Comparison of Calculated Data With T sts—Cable Section and Inductive Apparatus Connected to Secondary of 1,000-Kva Core-Form Transformer

Test Condition	Terminal Inductance (Henry)	Period of Oscillation (Micro-seconds)		Crest Voltage e_L in Per Cent e_H Crest	
		Calculated*	Experimental	Calculated*	Experimental
Figure 2I	$L_L=0.000354$	7.2	8.	1.2	1.1
Figure 2I	$L_L=0.000894$	10.	11.	2.4	2.6
Figure 2I	$L_L=0.001727$	12.	12.5	3.4	3.4
Figure 2I	$L_L=0.004400$	14.	14.	4.6	4.1

* Calculated data based on equation 4, and circuit constants in Table I, Figure 2I, and column 2 above. Crest voltage was determined from equation 4 simplified to $(2E/N)\cdot(L_L/L\ +L_L)$.

Application Problems

The problems encountered in practice are of a varied character, but in general they can be reduced to one of the several conditions in Figure 2 or to modifications and combinations of these. The electrostatic component need be considered only when terminals are left open or connected to an overhead line or other high-impedance circuits. The method is outlined in a previous section. In many of the cases examined, the electromagnetic component alone required attention. The transferred surge then is conducive to calculation according to the foregoing equations 2, 3, and 4, or similar derivations from the equivalent simplified circuit applicable to the problem (Figure 15). The crest voltage of the transferred surge is related to the turn-ratio relation, e_H/N, by a factor A which depends largely on the terminal conditions, the short-circuit inductance (L_S), and the wave of the surge applied (e_H). Typical values of the short-circuit inductance for power transformers are given in Table V.

Table V. Typical Short-Circuit Inductances (L_S) for Power Transformers Values Referred to Low Voltage

Line Voltages (Kilovolts)	Kva Per Transformer	Per Cent Impedance	L_S in Henrys*
220/13.2	20,000	14.8	0.00342
220/13.2	10,000	12.5	0.00578
154/13.2	10,000	11.7	0.00540
132/13.2	10,000	9.2	0.00425
110/13.2	10,000	8.7	0.00402
110/13.2	5,000	8.1	0.00748
66/13.2	10,000	8.1	0.00374
66/13.2	5,000	7.5	0.00692
66/13.2	2,500	7.0	0.01292
44/13.2	10,000	6.2	0.00286
44/ 6.6	2,500	6.2	0.00286
22/ 6.6	10,000	6.2	0.00072
22/ 6.6	2,500	6.2	0.00286

* These values correspond to 60-cycle ratings and for the low-voltage connected delta in three-phase bank. For wye connection reduce values to one third.

In practice, transformers usually are installed in three-phase banks, a condition which results in a transferred surge of equal or lower amplitude than for the single-phase connection previously discussed. For a surge e_H on one phase or simultaneous surges on two phases, the transferred voltage to ground is related to the single-phase transformer connection by the conversion factor K_A in Table VI for the various three-phase bank connections. Correspondingly, the duration of the front of the transferred surge is affected by the factor K_F indicated in the table. For simultaneous surges on all three phases, the electromagnetic component disappears (except for the wye–wye bank connection with both neutrals grounded). Thus the crest voltage of the transferred surge is given by the expression

$$e_L\,(\text{electromagnetic})=\frac{e_H}{N}\cdot A\cdot K_A\qquad(5)$$

A typical example will serve to illustrate the application of equation 5. A wood-pole line connects to a 115,000/13,200-volt, grounded-neutral wye–delta transformer bank. The secondary supplies, through a 125-foot cable, an industrial network which consists of a number of transformer units each feeding individual loads. On a single-phase basis, the circuit corresponds in effect to Figure 15F. In this case, the effective load inductance L_L and the short-circuit inductance L_S are of the same order so that the amplitude factor A in equation 4 is close to unity. The conversion factor for the three-phase bank is $K_A=0.33$. The advantage of arrester protection on the high side in limiting the transferred voltage is apparent. In the event of a severe lightning surge on the wood-pole line, a full-rated high-voltage arrester limits e_H to 450 kv or to a proportionally lower value, provided an 80 per cent arrester is permissible. Then from equation 5 the transferred voltage is (450/5)1x0.33 = 30 kv. In the absence of arrester protection on the high side, and should the industrial load become disconnected, the transferred surge across the secondary terminals and the cable may well reach 100 kv.

Similar problems arise in connection with rotating machines. These often supply transformer banks which connect on the high side to overhead lines exposed to lightning. The protection of rotating machines against surges requires that the voltage to ground at the machine should be limited to approximately the low-frequency test voltage of the machine, and, in order to limit the voltage between turns, the front of the surge appearing at the machine should as a rule exceed ten microseconds. The protection of machines is discussed in detail elsewhere.[8,9] The methods presented here are helpful in determining the transferred surge that may appear at the machine. In general, the transformer bank in conjunction with lightning arresters on the high side, as well as cable sections in the low-voltage circuit, usually provides substantial protection to machines. However, each case must be examined individually and on its merits. In important installations or for machines of older design located in vital zones, the addition of special protection at the machines may be justified.

In the problems on transferred surges that have been examined: in some, no protection was found necessary on the nonexposed circuit; in others, adequate protection on the exposed circuit also would provide protection to the nonexposed circuit; in some instances, the addition of protection on the nonexposed circuit appeared desirable. An analysis of the problem in each case provided the basis for sound engineering recommendations.

Appendix

Referring to Figure 15A where rectangular surge e_1 is applied to the primary winding over a transmission line and the secondary connects to a line, the transferred surge e_2 is derived from the circuit equations (expressed operationally)

$$e_1=L_1pi_1+Z_1i_1-Mpi_2$$
$$0=L_2pi_2+Z_2i_2-Mpi_1$$

The solution is

$$e_2=\frac{2MZ_2}{\sqrt{(L_1Z_2-L_2Z_1)^2+4Z_1Z_2M^2}}\times \epsilon^{-\frac{L_1Z_2+L_2Z_1}{2(L_1L_2-M^2)}t}\times \sinh\frac{\sqrt{(L_1Z_2-L_2Z_1)^2+4Z_1Z_2M^2}}{2(L_1L_2-M^2)}t\cdot e_1 \quad (6)$$

In the usual case $L_1 \gg M \gg L_2$, so that, in effect, equation 6 reduces to

$$e_2=\frac{M}{L_1}\left[1-\epsilon^{-\frac{L_1Z_2}{L_1L_2-M^2}t}\right]e_1 \quad (7)$$

Table VI. Factors Applied to e_L for Conversion From Single-Phase to Three-Phase Connections*

Bank connections high-voltage to low-voltage winding	Grounded wye to grounded wye Delta to grounded wye Delta to wye	Grounded wye to wye Wye to grounded wye Wye to wye	Delta to delta	Wye to delta Grounded wye to delta
K_A (amplitude)	1.00	0.67	0.67	0.33
K_F (front length)	1.00	1.00	0.33	0.33

* For derivation, see paper by Palueff and Hagenguth, reference 1; also see appendix of this paper. The conversion factors correspond here to turn ratio. Application is discussed in equation 5.

That is, in the usual practical problem Z_1 is of secondary or no importance because of the relatively high value of L_1. Circuit A simplifies to B or its equivalent C which, in terms of the turn ratio (N) and short-circuit inductance of the transformer (L_S), becomes circuit D. For a steep-front, exponential-tail surge e_H, the solution of e_L is equation 2.

Should the secondary in Figure 15A connect to a cable section of capacitance C_2, from the circuit

$$e_2=\frac{M}{(L_1L_2-M^2)C_2}\times\frac{p}{p^3+\frac{L_2Z_1}{L_1L_2-M^2}p^2+\frac{L_1}{(L_1L_2-M^2)C_2}p+\frac{Z_1}{(L_1L_2-M^2)C_2}}\cdot e_1$$

$$e_2=\frac{M}{(L_1L_2-M^2)C_2}\cdot\frac{p}{(p+\lambda)[(p+\beta)^2+\omega^2]}\cdot e_1$$

and the solution is

$$e_2=\frac{\frac{M}{(L_1L_2-M^2)C_2}}{(\lambda-\beta)^2+\omega^2}\times [\epsilon^{-\lambda t}+(\lambda-\beta)\epsilon^{-\beta t}\sin\omega t-\epsilon^{-\beta t}\cos\omega t]e_1 \quad (8)$$

For the common case $L_1 \gg M \gg L_2$, the circuit simplifies to Figure 15E. For a steep-front exponential-tail surge e_H, e_L is given by equation 3. Similarly, for a cable section and inductive apparatus, we have Figure 15F and equation 4. The transferred surge e_L for other terminal conditions can be derived in like manner.

For three-phase bank connections the solution of the transferred surge is the same form as for the single-phase, except that the conversion factors K_A and K_F appear, respectively, in the amplitude and time constant of the equations. Consider for instance the wye-wye connection (see Table VI), a rectangular surge e_1 applied to one terminal, and for the practical case considered here neglect Z_1. Current i_1 flows in the phase to which e_1 is applied and $i_1/2$ in the other two phases, all in the same direction. In the corresponding secondary windings the currents are i_2, $i_2/2$ and $i_2/2$. We then have the circuit equations

$$e_1=\frac{3}{2}L_1pi_1-\frac{3}{2}Mpi_2$$

$$0=\frac{3}{2}L_2pi_2-\frac{3}{2}Mpi_1+\frac{3}{2}Z_2i_2$$

The maximum voltage to ground is

$$e_2=i_2Z_2=\frac{2}{3}\cdot\frac{M}{L_1}\left[1-\epsilon^{-\frac{L_1Z_2}{L_1L_2-M^2}t}\right]e_1 \quad (9)$$

Thus for this connection $K_A=2/3$ and $K_F=1$.

For the wye–delta connection the circuit equations are

$$e_1=3/2L_1pi_1-3/2Mpi_2$$
$$0=L_2pi_2-Mpi_1+3Z_2i_2$$

and

$$e_2=\frac{1}{3}\cdot\frac{M}{L_1}\left[1-\epsilon^{-\frac{3L_1Z_2}{L_1L_2-M^2}t}\right]e_1 \quad (10)$$

so that, $K_A=1/3$ and $K_F=1/3$.

The conversion factors for other three-phase bank connections can be derived in a similar manner or by methods described elsewhere.[1] Their values for the various connections are given in Table VI.

References

1. Effect of Transient Voltages on Power-Transformer Design—IV. Transition of Lightning Waves From One Circuit to Another Through Transformers, **K. K. Palueff, J. H. Hagenguth.** AIEE Lightning Reference Book, page 931.

2. Model Tests on the Transfer of Surges to the Low-Voltage Winding of Transformers, **H. Neuhaus, R. Striegel.** *Wiss. Veröff Siemens-Konz*, volume 15, 1936, page 51.

3. Transfer of Impulse Voltages to the Low-Voltage Side of Three-Phase Transformers, **R. Elsner.** *Wiss. Veröff Siemens-Konz*, volume 16, 1937, page 1.

4. The Transfer of Overvoltages From the High-Voltage to the Low-Voltage Winding of Transformers, **M. Wellauer.** *Bulletin* Association Suisse des Electriciens, March 3, 1939, page 124.

5. Surge Phenomena (book), edited by British Electrical and Allied Research Association, Chapter XV, Transference of Surges From Primary to Secondary Transformer Windings Under No-Load Conditions.

6. Effects of Lightning Voltages on Rotating Machines and Methods of Protecting Against Them, **F. D. Fielder, E. Beck.** AIEE Lightning Reference Book, page 671.

7. Voltage Oscillations in Armature Windings Under Lightning Impulses—I, **E. W. Boehne.** AIEE Lightning Reference Book, page 598.

8. Surge Protection for Rotating Machines, **J. F. Calvert, A. C. Monteith, E. Beck.** AIEE Lightning Reference Book, page 1178. Also Surge Protection for Rotating Machines, **W. G. Roman, R. L. Witzke.** *Electric Light and Power*, July 1941, page 51.

9. Protection of Rotating A-C Machines Against Traveling-Wave Voltages Due to Lightning, **W. J. Rudge, Jr., W. Wieseman, W. W. Lewis.** AIEE Lightning Reference Book, page 1166.

10. Protecting Machines From Line Surges, **J. F. Calvert.** AIEE Lightning Reference Book, page 1408.

11. Transient Voltages in Rotating Machines, **E. M. Hunter.** AIEE Lightning Reference Book, page 1416.

12. Traveling-Wave Voltages in Cables, **H. G. Brinton, F. H. Buller, W. J. Rudge, Jr.** AIEE Lightning Reference Book, page 1133.

Effect of Wave Form on Let-Go Currents

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Synopsis: This paper is a continuation of "Effect of Frequency on Let-Go Currents"[2] and concludes studies made to determine let-go currents. This paper extends the analysis to cover various wave shapes and includes a method of determining reasonably safe currents for men, women, and children, for sine waves, direct current, and complex wave forms containing both a-c and d-c components. As in previous papers, conclusions regarding reasonably safe electric currents are based on the criterion that a safe current is the let-go current for 99$^1/_2$ per cent of a large group of healthy subjects.

ALTHOUGH it has been demonstrated that let-go currents, and hence the electric-shock hazard due to relatively small electric currents, are controlled by the crest value of an a-c wave and not by its root-mean-square value, the significance of this fact has received little attention. Previous papers on the general subject[1,2] have been concerned chiefly with developing a method of analysis and establishing concepts of reasonably safe sine-wave electric currents for various frequencies and for direct current. The procedure disclosed in this paper extends the analysis to permit estimating reasonably safe let-go currents for practically any commercial wave form, from sine-wave alternating current on one hand to pure direct current as the other extreme. The results of the analysis are believed to be important in increasing the safety of certain electronic devices, filters, and communication circuits, to the end that the sustained shock current obtained from parts of the circuit readily accessible to an operator may be held within reasonably safe or known limits. Also, the results may have value in specifying allowable leakage current to the chassis of radiobroadcast receivers; for improved design of high-voltage television power supplies; for defining the permissible magnitude of the components in the sustained output current from capacitor-discharge electric-fence controllers, and other similar apparatus. Although the conclusions have been based largely on experiments made with 60-cycle alternating current and direct current, the results may also be used for cases involving higher frequencies, but with increased factor of safety.

Results

Mean let-go current values obtained from tests made with various wave shapes follow the same curve if the crest value of the a-c component is plotted on one axis versus the d-c component on the other. Two conditions must be met in order to have the experimental data fall on the same curve. These conditions are:

1. The reference axis for measurement of the alternating component must be the average value or the direct component.
2. The peak or crest value of the alternating component must be measured in the direction of the maximum total current.

These requirements are illustrated in Figure 1, which gives sketches of the wave forms used in these experiments.

When measurements were made according to these two conditions, points representing the mean let-go current obtained from groups of subjects tested on sine-wave alternating current, direct current, and the four complex wave forms of Figure 1 fell closely on the top curve of Figure 2. The fact that the points fell on the same curve is important, because it permits estimating let-go currents for other wave forms as well as those used to locate the curve. Thus, from two simple measurements one may determine the probable shock hazard for electric currents of any assumed wave form. The 99.5 percentile curves for men and women were computed from equations 3 and 4, reference 2. In the absence of experimental data, the reasonably safe current for children was taken as 50 per cent of the safe values for men. Percentile curves for either more conservative or more liberal probabilities may be computed readily by using a similar procedure.

In these experiments sine waves were superimposed on pure direct current to give the wave forms illustrated in Figures 1B and 1C. In these tests crest *A* was equal to crest *B*, and the requirement that the peak of the alternating component be measured in the forward direction, or in the direction of the maximum total current, was unnecessary. The current wave of Figure 1B was similar but not identical to that obtained from the filtered output of an ordinary rectifier. Measurements were made to determine the alternating component of two rectifiers using a shunt and a crest vacuum-tube voltmeter calibrated to read milliamperes. Considerable difference in the magnitude of the opposite crests was obtained, which emphasized the importance of selecting the proper crest of the a-c ripple when using the method for practical problems.

The importance of determining the crest of the alternating component in the direction of the maximum total current is further illustrated from consideration of the wave forms of Figures 1D and 1E. These current waves represent the output from a half-wave and a full-wave rectifier, respectively. Consider crest *B* for either case. The vacuum-tube crest milliammeter indicated a definite reading when connected to measure crest *B*, even though the total current was zero. This

Table I. Effect of Wave Form on 180-Cycl Let-Go Current

Subject No.	Triangular Wave Crest Milliamperes	Triangular Wave Rms Milliamperes	Sine Wave Crest Milliamperes	Sine Wave Rms Milliamperes
2	22.1	12.8	21.4	15.1
12	22.6	13.0	21.9	15.5
21	30.0	17.3	42.4	30.0
22	38.7	22.3	36.3	25.7
23	25.2	14.5	23.2	16.4
27	21.2	12.2	20.1	14.2
30	37.1	21.4	31.7	22.4
31	25.5	14.7	25.2	17.8
32	26.0	15.0	23.3	16.5
36	26.0	15.0	25.7	18.2
37	34.4	19.8	32.5	23.0
40	27.2	15.7	25.5	18.0
45	26.8	15.5	23.5	16.6
54	27.7	16.0	32.8	23.2
55	30.3	17.5	26.0	18.4
61	27.4	15.8	29.4	20.8
64	30.0	17.3	30.0	21.2
67	24.0	13.8	22.9	16.2
74	24.1	13.8	23.8	16.
Mean	27.7	16.0	27.2	19.3

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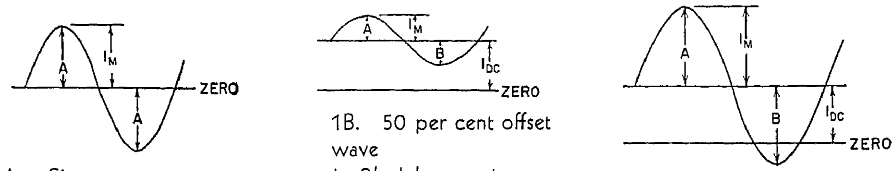
Data presented in this paper were obtained under the supervision of Major John B. Lagen, Medical Corps, United States Army, and Eric Ogden, professor of physiology, University of Texas Medical Branch, and consulting physiologist, John Sealy Hospital, Galveston, Tex. At the time this study was made Major Lagen was assistant professor of medicine and pharmacology, University of California Hospital, San Francisco, Calif., and Professor Ogden was associate professor of physiology, University of California Medical School, Berkeley, Calif. Acknowledgments are also given the men and women who volunteered for these experiments without whose interest and co-operation this work would have been impossible.



1A. Sine wave

$i = I_m \cos \omega t$

Crest $A = I_m = \sqrt{2}\, I$

1B. 50 per cent offset wave

$i = 2I_m + I_m \cos \omega t$

$I_{DC} = 2I_m$

Crest A = crest B

$= I_m = \sqrt{2}\, I$

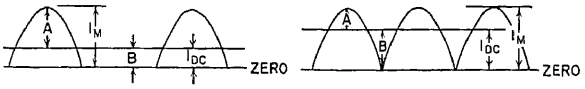
1C. 141 per cent offset wave

$i = \frac{I_m}{\sqrt{2}} + I_m \cos \omega t$

$I_{DC} = \frac{I_m}{\sqrt{2}}$

Crest A = crest B

$= I_m = \sqrt{2}\, I$



1D. Rectified half wave

$$i = \frac{I_m}{\pi} + \frac{I_m}{2} \cos \omega t + \frac{2I_m}{3\pi} \cos 2\omega t \ldots$$

I_{DC} = crest $B = \frac{I_m}{\pi}$

Crest $A = I_m - I_{DC}$

$= I_{DC}(\pi - 1) = 2.14\ I_{DC}$

1E. Rectified full wave

$$i = \frac{2I_m}{\pi} - \frac{4I_m}{3\pi} \cos 2\omega t - \frac{4I_m}{15\pi} \cos 4\omega t \ldots$$

I_{DC} = crest $B = 2\frac{I_m}{\pi}$

Crest $A = I_m - I_{DC} =$

$I_{DC}\left(\frac{\pi}{2} - 1\right) = 0.571\ I_{DC}$

Figure 1. A few of the wave forms used in the let-go-current tests

was due to the fact that the alternating component and the direct component were equal and opposite for the instant considered. It would be surprising indeed if let-go currents were controlled by crest *B*. As far as determined, there are only two important factors which control let-go currents. These are crest *A* of the alternating component and I_{DC}, the average value, or the direct component of the wave. The required measurements may be made easily with direct reading instruments. The direct component may be measured with an ordinary d-c milliammeter, and the alternating component may be measured with a half-wave crest vacuum-tube instrument in conjunction with a cathode-ray oscilloscope. The latter is used to permit selecting crest *A* instead of crest *B*. Of course, the values may be obtained from a calibrated oscillogram, but this procedure is cumbersome and requires photographic processes.

Although the curves of Figure 2 were determined from six series of tests using 60 cycles and direct current, it is believed that conclusions based thereon will be conservative for wave forms containing higher harmonics or other wave forms likely to be met in practice. Let-go currents for sine waves have a broad minimum from about 15 cycles to 100 cycles. The reasonably safe current curves of Figure 3 indicate that human tolerance increases slowly at first and then quite rapidly for frequencies below or above this range. Predictions made from the curves of Figure 2 for alternating components having frequencies outside the middle band in reality assume that the let-go-current curves are straight lines tangent to the curves of Figure 3 at their lowest points. The predictions may therefore be conservative and well on the side of safety.

The relative-discomfort curve of Figure 3 was obtained by plotting the reciprocals of the average let-go current values for each frequency, that is, the reciprocal of the corrected mean currents, Table I, reference 2. The vertical scale was arranged so that the discomfort would be 100 per cent for 60 cycles. The curve indicates the relative discomfort caused by a given current for the frequency range included in the graph. Although a subject's let-go current increases considerably at the very low frequencies, his muscles follow the current variations, and the sensations, presumably caused by the peaks of the current wave, are more painful than those experienced on the 60-cycle tests. At very high frequencies, sensations of heat rather than pain predominate. The term "relative discomfort" must therefore be given a broad interpretation, and, in the absence of a means of specifying varying degrees of pain, the curve may be taken to show in a general way the discomfort or the relative danger of a given current as a function of frequency.

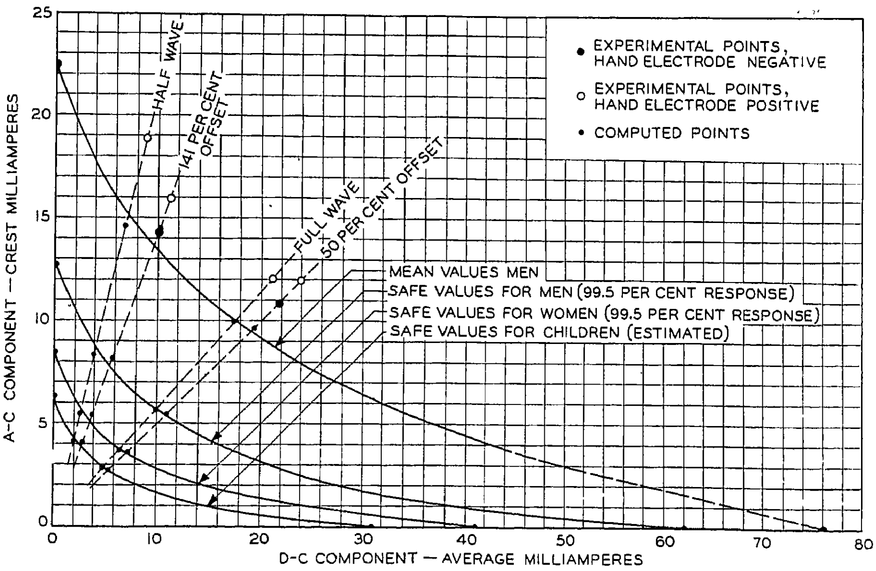
Experimental Data and Analysis

Effects of A-C Wave Form on Let-Go Currents

Before investigating the reactions produced by composite currents, it was considered desirable to make an additional investigation of the effect of wave form on let-go currents for complex alternating currents, that is, for wave forms containing no direct component. As previously reported, let-go currents appeared to be controlled by the crest and not by the root-mean-square (rms) value of the current. In these tests, comparisons were made for 60-cycle sine-wave current alone and in combination with a third harmonic equal to approximately $37^1/_2$ per cent of the fundamental. In one case, the third harmonic was phased so that the resultant wave was sharply peaked. For the other case, the third harmonic was shifted 180 degrees to produce a relatively flat-topped wave. Inspection of the data obtained from 26 subjects indicated that let-go currents were controlled by the peak of the current wave and not the root-mean-square value.[1]

A second series of tests was conducted subsequently in which let-go currents

Figure 2. Let-go-current curves plotted as a function of a-c and d-c components



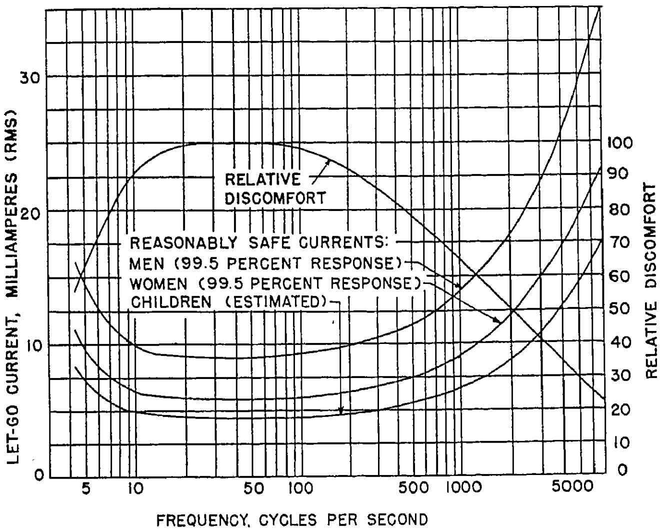


Figure 3. Sine-wave let-go currents and relative discomfort curves versus frequency

for 19 subjects were measured using sine and triangular waves of 180 cycles frequency. An oscillogram of the triangular current wave is shown in Figure 4. The triangular wave was obtained from a rheostat connected across the terminals of the interphase transformer of a small six-phase hot-cathode thyratron rectifier supplying a resistance load. Sine-wave 180-cycle currents were obtained from a beat-frequency oscillator and power amplifier. Experimental data for this test are given in Table I. The number 6 copper-wire electrode wet with salt solution was used for this test and for all other tests discussed in this paper. Although the let-go values were slightly more erratic than those observed for the 60-cycle wave-form tests, comparison of most individuals' values and the mean values for the group corroborate the conclusion that the crest of the a-c wave is the major factor which controls the let-go current.

Reasonably Safe 60-Cycle Sine-Wave Currents for Adults

The following data and analyses were taken from reference 2. The mean sine-wave 60-cycle let-go current for men was 15.87 rms milliamperes = 22.4 crest milliamperes. The 99.5 percentile = 15.87 (1−0.432) = 9 rms milliamperes = 12.7 crest milliamperes (from equation 1). The mean sine-wave 60-cycle let-go current for women was 10.5 rms milliamperes = 14.8 crest milliamperes. The 99.5 percentile = 10.5 (1−0.432) = 5.95 rms milliamperes = 8.4 crest milliamperes. These values were plotted on the vertical axis of Figure 2 to anchor the curves at this end of the graph.

Reasonably Safe 60-Cycle Sine-Wave Currents for Children

Obtaining let-go currents for young children appears difficult if not impossible of accurate determination. In addition to the reluctance of parents to permit experimentation on their children, limited experience indicates that children are likely to refuse to volunteer for a sufficient number of trials to permit determination of their let-go current with any degree of accuracy. As previously mentioned[2] let-go currents appear related to the muscular development of the wrist and forearm. However, all attempts to find a correlation, other than general appearance, with age, weight, strength of grip, or arm measurements were without conclusive results. In view of the early development of the grip of children it might be expected that their let-go currents would be relatively large; moreover it is a common observation that children appear to be very hardy in their ability to withstand a variety of conditions which would be disastrous to adults. In the interest of establishing a reasonably safe current for normal healthy children, and in the absence of experimental data, it seemed reasonable to estimate the probable safe current for children as 50 per cent of the safe value for normal adult males. Reactions caused by currents of this magnitude were unobjectionable to the men, and they were found to be amply safe for three small boys tested. The following are the 60-cycle sine-wave let-go currents obtained on the boys:

Age 5 years.....Let-go current 7 rms milliamperes[*]
Age 9 years 3 months..Let-go current 7.6 rms milliamperes
Age 10 years 11 months..Let-go current 9 rms milliamperes

Accordingly, the reasonably safe sine-wave value for normal children was com-

Table II. Let-Go Currents—Sine-Wave and 141 Per Cent Offset-Wave Tests

Hand Electrode Positive			Hand Electrode Negative		
Subject No.	Sine-Wave Control 60-Cycle (Rms Milliamperes)	Offset-Wave A-C Component (Rms Milliamperes)	Subject No.	Sine-Wave Control 60-Cycle (Rms Milliamperes)	Offset-Wave A-C Component (Rms Milliamperes)
73	9.2	7.7	73	9.2	7.5
104	12.6	10.0	104	12.6	8.0
128	13.2	10.0	123	17.8	8.8
93	15.9	11.2	93	15.9	9.3
99	15.8	11.6	99	15.8	9.4
130	16.9	12.2	130	16.9	10.4
129	16.5	12.7	124	21.5	10.5
134	15.1	13.0	132	19.4	11.5
132	19.4	13.0	128	13.2	11.6
			129	16.5	12.0
			134	15.1	12.3
Mean	14.96	11.27		15.81	10.12

Corrected mean:
D-c average and a-c rms..11.27×15.87/14.96 = 11.96 milliamperes .10.12×15.87/15.81 = 10.16 milliamperes (equation 2, reference 2)
A-c crest...............$\sqrt{2}$×11.96 = 16.91 milliamperes..........$\sqrt{2}$×10.16 = 14.37 milliamperes (see Figure 1C)

99½ percentile:
A-c crest men..........16.91(1.0−0.432) = 9.60 milliamperes....14.37(1.0−0.432) = 8.16 milliamperes
D-c average men.........11.96(1.0−0.432) = 6.79 milliamperes...10.16(1.0−0.432) = 5.77 milliamperes (equation 3, reference 2)
A-c crest children.......0.5×9.60 = 4.8 milliamperes.............0.5 × 8.16 = 4.1 milliamperes
D-c average children.....0.5×6.79 = 3.4 milliamperes.............0.5 × 5.77 = 2.9 milliamperes
A-c crest women........9.60×10.5/15.79 = 6.4 milliamperes......8.16 × 10.5/15.87 = 5.4 milliamperes
D-c average women......6.79×10.5/15.87 = 4.5 milliamperes......5.77×10.5/15.87 = 3.8 milliamperes (equation 4, reference 2)
Polarity effect............10.16/11.96 = 0.85

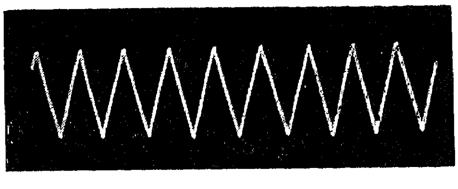


Figure 4. Triangular current wave used to determine effect of wave form on let-go currents for complex a-c wave forms

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[*] Determined by Royce E. Johnson, director, electrical standards laboratory, University of Wisconsin, Madison, Wis.

puted as 0.50×9=4.5 rms milliamperes=6.3 crest milliamperes.

Reasonably Safe Direct Currents

The following d-c release currents were taken from reference 2:

Men, corrected average = 76.1 d-c milliamperes
99.5 percentile = 62.0 d-c milliamperes
Women, 99.5 percentile = 41.0 d-c milliamperes

Using the reasoning of the preceding paragraph, the reasonably safe direct current for healthy children was computed as 0.50×62.0=31.0 d-c milliamperes. These values were plotted on the horizontal axis of Figure 2 to fix the lower limits of the curves. At the conclusion of the a-c let-go-current tests, the two boys used in this investigation released the following direct currents easily and without comment. Higher values were not attempted:

Age 9 years 3 months...15 d-c milliamperes
Age 10 years 11 months............26 d-c milliamperes

141 Per Cent Offset-Wave-Form Let-Go-Current Tests

In these tests 60-cycle sine-wave alternating current was superimposed on direct current to produce the current wave forms shown in Figure 1C. The proper proportion of the alternating current to direct current was maintained by connecting an ordinary d-c milliammeter in series with a shunt to which was connected a General Radio type 726A vacuum-tube voltmeter. Adjustments were made so that the d-c milliamperes were always equal to the root-mean-square a-c milliamperes, resulting in a wave having a ripple equal to

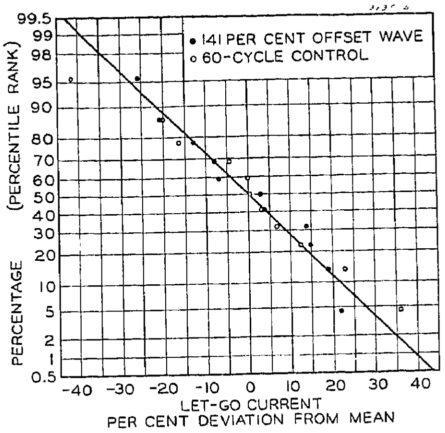

Figure 5. 141 per cent offset-wave let-go-current deviation curve

Hand electrode negative

$$\text{Ripple} = \frac{\text{crest a-c milliamperes}}{\text{average milliamperes}} = \frac{\sqrt{2}\ \text{rms milliamperes}}{\text{d-c milliamperes}} = 1.41$$

Experimental values and analysis are given in Table II, and the corresponding deviation curves are shown in Figures 5 and 6. Three determinations of let-go currents were made; one test with the wire electrode negative, one with the wire electrode positive, and one test with commercial 60-cycle sine wave to serve as a control. The three tests were conducted on the same afternoon to obtain comparable results. The sequence of the tests was alternated among the subjects to randomize the effects of fatigue. It will be noted that the experimental points fell closely about the 60-cycle standard-deviation curve. This was interpreted to mean that a sufficient number of subjects had been used to permit predictions of acceptable accuracy to be made for a large group of subjects. The corrected mean of the sample was computed from equation 2, reference 2. Equations 3 and 4, reference 2, were then used to predict reasonably safe currents for men and women. The reasonably safe current for children was taken as 50 per cent of the safe value for men, using the data obtained with the wire electrode negative to give conservative results. Experimental and computed points for the wire negative were then plotted on Figure 2 to assist in locating the mean and safe let-go current curves.

50 Per Cent Offset-Wave-Form Let-Go-Current Tests

A similar procedure was used to determine the let-go currents for a wave having 50 per cent a-c ripple. For these tests the proportion of alternating current to direct current was maintained so that the d-c milliammeter read exactly twice the crest a-c milliamperes. Experimental values and analyses are given in Table III and the corresponding deviation curves in Figures 7 and 8.

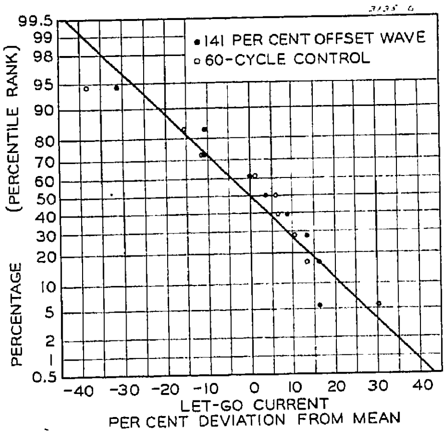

Figure 6. 141 per cent offset-wave let-go-current deviation curve

Hand electrode positive

Although the experimental points (especially the 60-cycle control points) did not follow the 60-cycle standard-deviation curve as closely as desired, this was the best that could be done with the available data. At the beginning of the investigation some six years ago, it was planned to obtain values for at least 25 subjects for each test in order to eliminate possible criticism due to deriving conclusions from

Table III. Let-Go Currents—Sine-Wave and 50 Per Cent Offset-Wave Tests

Hand Electrode Positive			Hand Electrode Negative		
Subject No.	Sine-Wave Control 60-Cycle (Rms Milliamperes)	Offset-Wave A-C Component (Rms Milliamperes)	Subject No.	Sine-Wave Control 60-Cycle (Rms Milliamperes)	Offset-Wave A-C Component (Rms Milliamperes)
123	17.5	7.2	93	17.2	6.0
125	19.3	7.7	123	17.5	6.3
93	17.2	7.9	132	18.3	6.8
82	16.2	8.3	125	19.3	7.1
129	15.1	8.4	129	15.1	7.8
132	18.3	8.6	130	17.6	7.8
135	16.7	8.6	82	16.2	8.1
124	21.5	9.2	124	21.5	8.3
130	17.6	9.3	135	16.7	8.6
134	19.8	9.5	134	19.8	9.8
Mean:	17.92	8.47		17.92	7.66
Corrected mean:					
A-c crest	$\sqrt{2}\times 8.47\times 15.87/17.92 = 10.61$ milliamperes			$\sqrt{2}\times 7.66\times 15.87/17.92 = 9.60$ milliamperes (equation 2, reference 2)	
D-c average	$2\times 10.61 = 21.22$ milliamperes			$2\times 9.60 = 19.20$ milliamperes (see Figure 1B)	
99½ percentile:					
A-c crest men	$10.61(1-0.432) = 6.02$ milliamperes			$9.60(1-0.432) = 5.45$ milliamperes	
D-c average men	$21.22(1-0.432) = 12.05$ milliamperes			$19.20(1-0.432) = 10.92$ milliamperes (equation 3, reference 2)	
A-c crest children	$0.5\times 6.02 = 3.0$ milliamperes			$0.5\times 5.45 = 2.7$ milliamperes	
D-c average children	$0.5\times 12.05 = 6.0$ milliamperes			$0.5\times 10.92 = 5.5$ milliamperes	
A-c crest women	$6.02\times 10.5/15.87 = 4.0$ milliamperes			$5.45\times 10.5/15.87 = 3.6$ milliamperes	
D-c average women	$12.05\times 10.5/15.87 = 8.0$ milliamperes			$10.92\times 10.5/15.87 = 7.2$ milliamperes (equation 4, reference 2)	
Polarity effect	$9.60/10.61 = 0.90$				

Table IV. Let-Go Currents—Sine-Wave and Six Per Cent Offset-Wave Tests

Hand Electrode Negative

Subject No.	Sine-Wave Control 60-Cycle (Rms Milliamperes)	Offset-Wave Components		Remarks
		A-C Rms Milliamperes	D-C Average Milliamperes	
45	16.2	2.3	53	Let-go current
134	15.1	2.9	70	Limit of endurance
140	15.8	3.2	73	Limit of endurance
Mean:	15.7	2.8	65.3	

A-c crest $= 2.8\sqrt{2} = 3.96$

Per cent ripple $= \frac{3.96}{65.3} = 0.06$

insufficient data. Conclusions based on available data are therefore submitted, subject to slight adjustments, should this be found desirable when conditions permit resumption of the project. Experimental research was stopped when the nation became involved in the war, and it will not be resumed for the duration.

Six Per Cent Offset-Wave-Form Let-Go-Current Tests

The results of let-go current tests made with a composite wave having a ratio of crest alternating current to direct current equal to 0.06 are given in Table IV. This was the last test made before the experiments were terminated, and only three subjects were used. Three values are too few in number to warrant drawing conclusions, and the point representing this test was omitted from Figure 2.

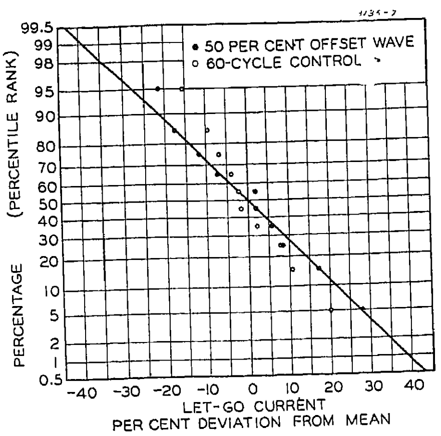

Figure 7. 50 per cent offset-wave let-go-current deviation curve

Hand electrode negative

However, if the point representing the average for the group is inserted on Figure 2, it will be found to be well above the mean curve. This is consistent with the conjecture that the lower end of the curve, which was determined from tests using pure direct current, is well on the side of safety. These data are instructive, since one of the three subjects reached his let-go limit while the other two did not. Although two subjects could still release the electrode, they refused higher currents because of the severity of the shock at the instant of releasing the electrode. This is the only case where let-go currents and release currents were encountered in the same test. These observations stress the importance of considering the alternating component, regardless of how small it may be in comparison to the direct component, when studying shock hazards. Sensations produced by the filtered output of a *B* eliminator, in which the a-c ripple was so small that it was thought negligible, were much more painful than the same current obtained from a battery.

Table VI. Sine Wave and Rectified Full-Wave Tests

Hand Electrode Positive

Subject No.	Sine-Wave Control 60 Cycle (Rms Milliamperes)	Rectified Full-Wave (Average Milliamperes)
82	12.2	15.0
128	13.5	16.0
123	16.2	18.5
45	16.2	18.5
131	16.8	19.0
132	16.1	19.8
129	13.5	20.2
122	19.8	21.0
124	18 2	21.5
130	14.6	22.1
125	17.3	23.4
109	20.0	26.4
37	20.9	26.4
105	23.0	27.5
Mean:	17.02	21 09

Corrected mean:
D-c average men.... 21.99×15.87/17.02 = 19.66 milliamperes, hand electrode positive (equation 2, reference 2)
A-c crest men.... 0.571×19.66 = 11.22 milliamperes (see Figure 1E)
D-c average men.... 0.875×19.66 = 17.20 milliamperes, hand electrode negative (using polarity correction factor = 0.875)
A-c crest men.... 0.571×17.20 = 9.82 milliamperes, hand electrode negative (see Figure 1E)

$99^1/_2$ percentile:
A-c crest men.... 9.82(1−0.432) = 5.58 milliamperes, hand electrode negative
D-c average men.... 17.20(1−0.432) = 9.77 milliamperes, hand electrode negative (equation 3, reference 2)
A-c crest children.... 0.5×5.58 = 2.8 milliamperes, hand electrode negative
D-c average children.... 0.5×9.77 = 4.9 milliamperes, hand electrode negative
A-c crest women.... 5.58×10.5/15.87 = 3.7 milliamperes, hand electrode negative
D-c average women.... 9.77×10.5/15.87 = 6.5 milliamperes, hand electrode negative (equation 4. reference 2)

Table V. Let-Go Currents—Sine-Wave and Rectified-Half-Wave Tests

Hand Electrode Positive

Subject No.	Sine-Wave Control 60-Cycle (Rms Milliamperes)	Rectified Half-Wave (Average Milliamperes)
45	18.5	6.7
77	14.4	7.0
126	17.8	8.5
129	16.9	8.6
130	18.5	8.9
125	17.3	9.1
132	15.6	9.1
124	17.3	9.5
37	20.9	9.8
21	25.1	10.0
93	17.1	10.1
Mean:	18.13	8.84

Determination of polarity correction factor

From Table II	0.85
From Table III	0.90
Mean	0.875

Corrected mean:
D-c average men.... 8.84×15.87/18.13 = 7.74 milliamperes, hand electrode positive (equation 2, reference 2)
A-c crest men.... 2.14×7.74 = 16.57 milliamperes, hand electrode positive (see Figure 1D)
D-c average men.... 0.875×7.74 = 6.77 milliamperes, hand electrode negative (using polarity correction factor = 0.875)
A-c crest men.... 2.14×6.77 = 14.49 milliamperes, hand electrode negative (see Figure 1D)

$99^1/_2$ percentile:
A-c crest men.... 14.49(1−0.432) = 8.22 milliamperes, hand electrode negative
D-c average men.... 6.77(1−0.432) = 3.85 milliamperes, hand electrode negative (equation 3, reference 2)
A-c crest children.... 0.5×8.22 = 4.1 milliamperes, hand electrode negative
D-c average children.... 0.5×3.85 = 1.9 milliamperes, hand electrode negative
A-c crest women.... 8.22×10.5/15.87 = 5.4 milliamperes, hand electrode negative
D-c average women.... 3.85×10.5/15.87 = 2.5 milliamperes, hand electrode negative (equation 4, reference 2)

Rectified Full- and Half-Wave Let-Go-Current Tests

Let-go current tests were conducted, using the output of a single-phase mercury-arc rectifier. The results obtained using one anode are given in Table V and Figure 9. Similar data obtained for both anodes are given in Table VI and Figure 10. These tests were conducted with the hand electrode positive. Sixty-cycle sine-wave let-go tests were also conducted to serve as a control for computing the response for a large group, as previously discussed.

It was found that the sensations produced by the current were more painful and the let-go current was less when the hand electrode was negative in comparison to tests made with the hand electrode positive. The tests using the rectified waves were conducted with the hand electrode positive, and it was necessary to determine a correction for polarity, in order to compare the points representing these tests with the preceding data. The effect of polarity was determined from the 141 per cent and the 50 per cent offset-wave tests. A polarity correction factor was taken equal to the ratio (mean let-go current, hand electrode negative) ÷ (mean let-go current, hand electrode positive). The average ratio obtained from these two tests was taken as the polarity correction factor = 0.875. This factor was used in the analysis of the data of Tables V and VI, and the points determined thereby were given due consideration in locating the curves of Figure 2.

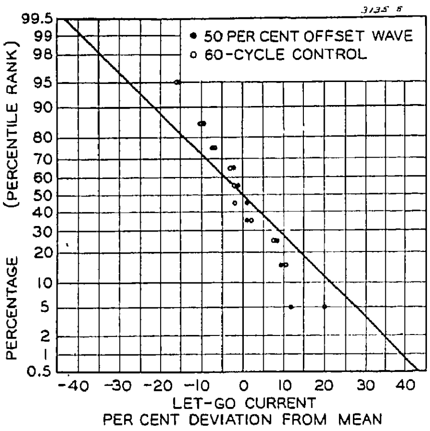

Figure 8. 50 per cent offset-wave let-go-current deviation curve

Hand electrode positive

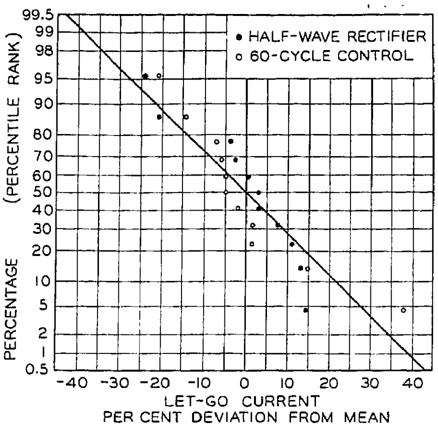

Figure 9. Rectified half-wave let-go-current deviation curve

Hand electrode positive

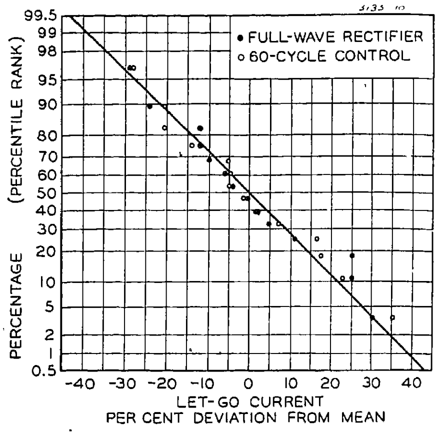

Figure 10. Rectified full-wave let-go-current deviation curve

Hand electrode positive

The polarity effect, which was found to decrease the average let-go currents $12^{1}/_{2}$ per cent when the hand electrode was negative, may have value from a safety standpoint. The decrease in both the sensations and the shock hazard for a given current when the hand electrode is positive may be helpful in arriving at a decision whether to ground the positive or the negative side of a circuit.*

As can be seen from inspection of Figure 10, the experimental points for the full-wave-rectifier test followed the 60-cycle standard-deviation curve closely. In comparison, the points for the half-wave-rectifier test were somewhat more scattered about the deviation curve (see Figure 9). It should be noted that the scattering about the deviation curve was about the same for both the wave form under investigation and the 60-cycle control. The scattering was explained as due to the limited number of subjects tested rather than to suggesting a different response for rectified half-wave currents. It is believed that data from a larger group of subjects would have reduced the discrepancies and resulted in the points following the 60-cycle deviation curve as closely as that found for the other tests.

Smooth curves were drawn through the computed points (wire negative) to give the curves of Figure 2. Several other attempts were made to analyze the data, but for each case tried at least some of the computed points failed to follow a common curve. Points obtained from the analysis described in the foregoing paragraphs closely follow the same curve, and for this reason it is believed that the procedure is sufficiently general to permit predicting reasonably safe currents with acceptable accuracy for wave shapes similar as well as those actually used in the experiments. Although one must use caution when arriving at conclusions, the good agreement between the experimental points and the theory suggest that the analysis should be valid for predictions involving complex or composite waves containing a-c components and d-c components in the proportions likely to be met on commercial apparatus or associated circuits. As previously discussed, it is believed that errors due to wave forms containing high-frequency components should be on the safe side, and the resulting currents should be on the side of safety.

References

1. Electric Shock, **Charles F. Dalziel, John B. Lagen, Joe L. Thurston.** AIEE Transactions, volume 60, 1941, pages 1073–9; discussion pages 1295–7.

2. Effect of Frequency on Let-Go Currents, **Charles F. Dalziel, Eric Ogden, Curtis E. Abbott,** AIEE Transactions, volume 62, 1943, December section, pages 745–50.

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* It is well known to physiologists that during passage of direct current through a nerve the regions in the neighborhood of the electrodes are altered with respect to

1. Sensitivity to stimulation.
2. Ability to conduct nerve impulses.

The negative region becomes more sensitive and a better conductor.

Effect of Frequency on Let-Go Currents

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Synopsis: This paper on electric shock covers the subject of sine-wave let-go currents for both men and women and contains an analysis which permits improved accuracy in predicting the response for large groups based on experiments made on a relatively small number of subjects. It should be of especial interest to persons who have had accidents in which they barely escaped "freezing" to an electrified conductor and also to those interested in electrical safety. The range of frequencies covered is from 5 to 10,000 cycles and also direct current. The paper is the most comprehensive treatment of the subject yet published as the analysis permits predicting currents of a specified degree of safety for both men and women for this wide frequency range.

THIS paper presents the results of a study to determine the effect of frequency on let-go currents made on 134 men and 28 women at the University of California. Let-go currents are important, because it has been found that an individual can withstand, with no ill aftereffects, repeated exposure to his let-go current for at least the time required for him to release the conductor. Larger currents are dangerous, because release from the circuit during accidental contact is problematical. A reasonably safe current for normal men and women is defined as the let-go current for 99$^1/_2$ per cent of a large group. A knowledge of let-go currents is important for the formation of safety codes, for explaining accidents, and for the design of electrical devices with exposed electrodes.

The majority of the data was presented in a preliminary report before the Institute in 1941.[1] The number of male subjects for the 60-cycle test now totals 134, the data on females are new, and an improved analysis is disclosed. Use of the new analysis permits improved accuracy in predicting results for a large group from tests made on a small but representative sample. The paper includes an example in which the method is used to predict probable let-go currents for all normal men and women for a frequency range from 5 to 5,000 cycles. Estimates are given for 10,000 cycles and also for direct current.

Determination of Let-Go Currents

An individual's let-go current is defined as the maximum current he can tolerate when holding a copper conductor in one hand and yet let go of the conductor by using muscles directly affected by that current. No material changes in experimental procedure, safety precautions, selection of subjects, or equipment were found to be necessary, and, in general, the experiments were continued as described in the preliminary report. The following résumé is included for convenience. The subjects held and then released a test electrode consisting of a number 6 copper wire, and the circuit was completed by placing the other hand or foot on a flat brass plate, or by clamping a conducting band lined with moist cloth firmly on the upper arm. After one or two preliminary trials to accustom the subject to the sensations and muscular contractions produced by the current, the current was increased to a certain value and the subject was commanded to let go of the wire. If he succeeded, the test was repeated at a slightly higher value. If he failed, a lower current was used, and the values were again increased until the subject could no longer release the test electrode. The end point was checked by several trials. The agreement between those trials was within about two to five per cent, and the highest value was taken as the individual's let-go value in order to eliminate the effects of fatigue.

The tests reported here were conducted with hands wet with a common saltwater solution to secure uniform conditions and to reduce the sensation of burning caused by high current densities at tender spots and at the instant of releasing the electrode. Other tests were made with dry hands, hands moist from perspiration, and hands dripping wet from weak acid solutions. The effect of the size of the electrodes was also investigated. As reported previously, the location of the indifferent electrode, the moisture conditions at the points of contact, and the size of the electrodes had no appreciable effect on an individual's let-go current. It is believed that conclusions based on tests using the wet copper electrode may be used to predict reasonably safe electric currents with an accuracy sufficient for most practical purposes.

There was considerable variation in an individual's value on succeeding trials, the trend usually being toward higher values. Therefore the largest current released on the *first* test on any frequency was taken as the let-go current for that frequency. This was done to include the element of surprise to as great an extent as possible and to give conservative results. Psychological factors, especially fear and competitive spirit, were the most important causes for the variations. Physiological factors also played an important part, but so far their exact mechanism remains unknown. It seemed that let-go currents in both sexes were related to the muscular development of the wrist and forearm. Husky subjects having low let-go values could almost invariably be persuaded (or heckled by other subjects) to continue the test until their values were in line with others of similar physique. Although attempts were made to quantitate this relationship by correlating let-go currents with the strength of grip and with forearm and wrist diameters, the results were inconclusive. Perhaps this was because of the narrow range and lack of precision involved in the physiological measurements; moreover, forearm and wrist diameters are determined to an uncertain degree by fat and bone as well as by muscular tissue.

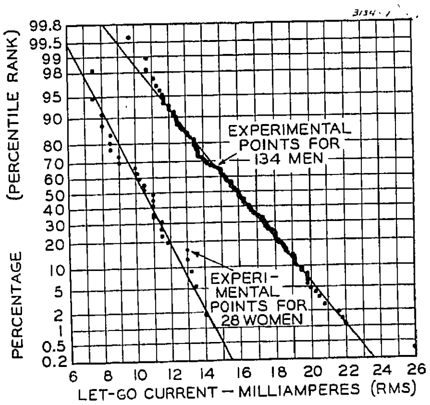

Figure 1. 60-cycle let-go-current distribution curves for men and women

Electric currents of a specified degree of safety for normal men or women may be obtained directly from the curves

Sixty-cycle let-go currents were meas-

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ured on 27 additional women, bringing the total to 28. The subjects ranged in age from the late teens to the early twenties, were light in stature, and obviously not accustomed to hard physical work, and their forearm muscles were not particularly well developed. Although the women volunteered freely for the tests, it proved impossible to develop enthusiasm or any degree of competition at high currents. The results are probably representative for the sedentary type; however, from observation of the reactions of subjects having the greatest muscular development, it is likely that the values were considerably lower than those which would have been obtained, had a typical group of mature healthy women used to physical labor been used. Results based on these data therefore should be conservative and on the side of safety.

A Safe Electric Current

The principle of biological variability is recognized so universally that no attempt should be made to specify any electric current as safe for all people. The press contains frequent accounts of fatalities ascribed to heart failure caused by overexcitement, intense emotion, or shock (shock of injury, *not* electric shock). Some of the subjects volunteering for these tests became frightened and trembled all over; some even complained of pain when holding the test electrode before the current was turned on. Although these persons were not used in the experiments, the experiences dramatically illustrated the possibility that a person with a diseased heart might succumb from any contact, or even the fear of contact, with an electric circuit. This possibility must be recognized, and an occasional death is to be expected as a result of casual contact involving electric currents known to be harmless to the great majority of healthy individuals.

Quite aside from determining an absolutely safe electric current for all human beings is the practical problem of determining a current which would be reasonably safe for most normal healthy adult men and women. In certain cases, economics or special applications may necessitate the use of current values which are hazardous to only a small percentage of a large group. This is particularly important at the present time, because of the widespread use of electric devices having exposed electrodes.[2,3]

Several factors must be considered in deciding what constitutes a reasonably safe electric current. The victim of a severe accidental electric shock can often release himself by using muscles little affected by the current. The muscular reactions produced by the current may tend to break the circuit rather than to improve it, or loss of balance may free the victim. However, it is doubtful if any of these methods would be of avail when contact is established by gripping an electrified conductor so that the path of the current was across the body. The hazard due to electric shock depends upon several factors, the most important being the magnitude of the current, the current pathway through the body, the time of contact, and the physical condition of the victim.

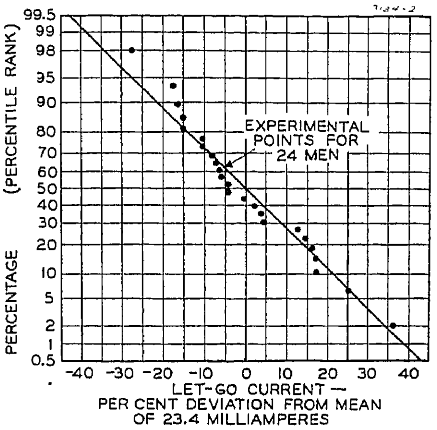

Figure 2. Five-cycle deviation curve for men

Conclusions regarding reasonably safe electric currents were based on the factors mentioned in the preceding paragraphs, together with the results of carefully conducted experiments made on a total of approximately 250 men and 28 women, and involved several thousand individual tests. Without a single exception, it was found that an individual could withstand, with no ill aftereffects, repeated exposure to his let-go current for at least the time required for him to release the conductor. The current pathway for the majority of the tests was between the hands. A few experiments were made with current pathway between one hand and one or both feet; in other tests the pathway was between the hand and an arm band.[2,3]

It is the authors' studied opinion that, for most practical purposes, the maximum electric current which is safe for an individual is the greatest current he can release by using muscles directly affected by that current. From this, it was concluded that a reasonably safe electric current for most normal healthy physically fit adult men and women is the let-go current for $99^1/_2$ per cent of a large group. Numerical determination of reasonably safe currents from the let-go current tests will be discussed later at an appropriate point in the text.

Analysis of Let-Go Currents

Let-go currents were obtained for groups of male subjects ranging in number from 25 to 134 using sine-wave alternating currents from 5 to 10,000 cycles and also direct current. The let-go currents for each test were then plotted on probability cross-section paper, and the straight line governed by the majority of the points was drawn to obtain the normal distribution curve for the sample. An advantage of this method is that points obviously not conforming to the sample may be disregarded by inspection. The procedure is illustrated in Figure 1 which gives the 60-cycle let-go-current distribution curves for 134 men and 28 women. Distribution curves for the other frefrequencies were similar, except for the current scale or the slope of the distribution curve.

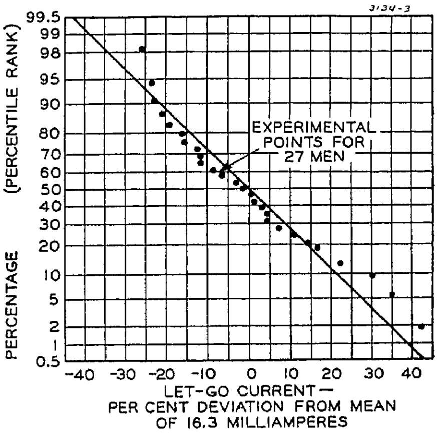

Figure 3. Ten-cycle deviation curve for men

It was found that, when let-go currents were plotted as per cent deviations from the mean of any test, the slope of all the resulting straight lines was the same. This was true for both men and women on the 60-cycle tests and for other tests from 5 to 5,000 cycles. Deviation curves for the 60-cycle tests are given in Figures 5 and 5A. Since only a small number of subjects was used for other than the 134-men 60-cycle test, the straight line response of Figure 5 was drawn on each curve as the probable response for the other frequencies also. Within the range of 5 to 5,000 cycles, the straight line representing the 60-cycle response was the best line that could be drawn through the majority of the points (see Figures 2 to 10 inclusive). The fact that let-go current values follow the same deviation curve may be illustrated in a rather striking manner if all the points (excluding the 60-cycle values, to avoid confusion) are plotted on the same curve sheet. The wide discrepancies of one or two points from the deviation curves were due to the relatively small number of subjects used. The reason that let-go currents follow the same curve for this range of frequencies may be related to the similarity of the physiological phenomena, which were characterized by painful muscular contractions. Outside of this frequency range, sensations of heat predominate. Strictly comparable measurements are not possible for direct current.

In previous studies, the mean of a test was taken as the most reliable measure for the sample, and the accuracy of prediction was chiefly dependent upon the number of subjects used for a particular test. Improved accuracy for small samples results from plotting the data as percentage deviations from the mean of the sample and comparing the response with the slope of the 134-men 60-cycle deviation curve of Figure 5. In the five-cycle test, shown in Figure 2, two points deviated from the rest of the data sufficiently to suggest that they should be discarded. Accordingly, the points were recomputed after the data from these two subjects had been rejected, leaving a total of 24 points. Such rejection was not found to be necessary in the other tests.

Predictions for any percentile rank are obtained from

$$\text{Percentile rank} = \text{mean of sample} \pm \text{deviation from mean} \times \text{mean of sample}$$

or

$$\text{Percentile rank} = \text{mean of sample } (1 \pm \text{deviation from mean}) \quad (1)$$

It is evident that the accuracy of prediction depends to a large extent upon the accuracy of the 134-men 60-cycle deviation curve and upon the subjects being a true random sample of the population. The data upon which this curve was constructed were obtained from carefully conducted tests made during a six-year period. Although care was taken to obtain only subjects in good physical condition, they were not selected especially with respect to age, weight, or muscular development. They represented a wide variety of normal men, with an age range of from 21 to 46. The close agreement between the 134 points and resulting straight line indicates that the response actually followed a normal distribution. This is exactly what should have been observed, had an infinite sample been tested. This close agreement is presented to substantiate the conclusion that a sufficient number of subjects has been examined to permit determination of the response of a large sample. Proceeding to the limit, it is assumed that a sufficient number of subjects has been tested to obtain the representative response of all normal men with an accuracy sufficient for engineering purposes.

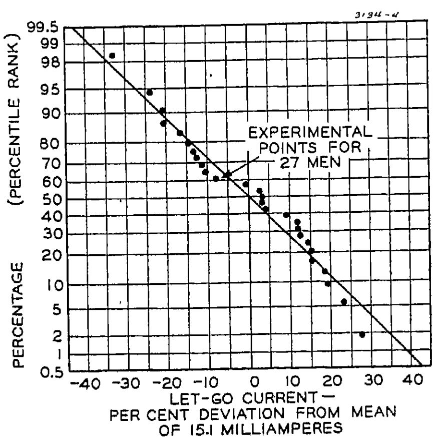

Figure 4. 25-cycle deviation curve for men

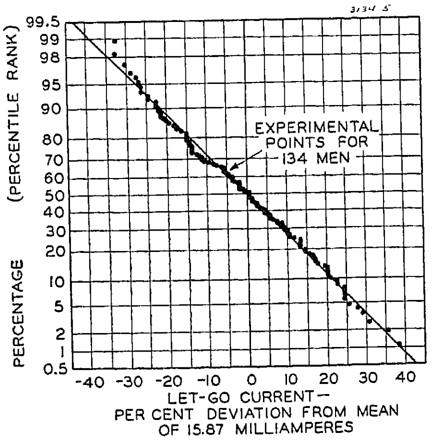

Figure 5. 60-cycle deviation curve for men

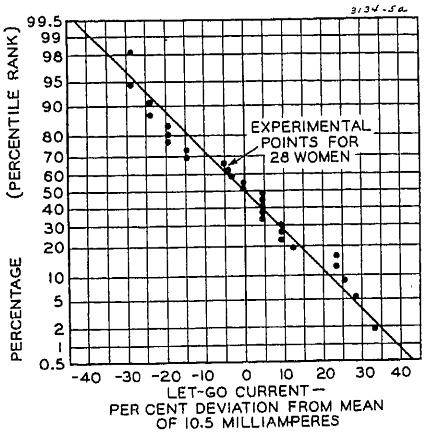

Figure 5A. 60-cycle deviation curve for women

An important factor controlling the accuracy of predictions for other tests is the necessity of procuring a representative sample. It is entirely possible that any small sample may have a mean considerably above or below that of an average sample, irrespective of agreement with the standard deviation curve. This is particularly important in experiments such as this, because it was usually found that let-go currents on the same frequency had a tendency to increase as the number of tests increased. This was attributed to the subject's becoming accustomed to the ordeal. Let-go-current tests should be conducted for the given test conditions and also on sine-wave 60 cycles to serve as a control. The order of the tests among the subjects should be changed frequently to randomize effects of fatigue.

The corrected mean, that is, the mean for an infinite sample equals

$$\text{Corrected mean of sample} = \frac{\text{Mean 60-cycle let-go current of all men}}{\text{Mean 60-cycle let-go current of sample}} \times \text{mean of sample for given test conditions}$$

or

$$\text{Corrected mean of sample} = \frac{15.87}{\text{60-cycle control}} \times \text{mean of sample} \quad (2)$$

The percentile rank for an infinite group is obtained from

$$\text{Percentile rank} = \text{corrected mean of sample} \times (1 \pm \text{deviation from mean}) \quad (3)$$

For example, the $^1/_2$ or $99^1/_2$ per cent response for any frequency between 5 and 5,000 cycles is given by

Deviation from the mean for $^1/_2$ per cent or $99^1/_2$ per cent $= \pm 0.432$ (from Figure 5)

$^1/_2$ or $99^1/_2$ percentile rank $=$ corrected mean of sample $\times (1 \pm 0.432)$

Figure 14 shows the effect of frequency on let-go currents. Significant data are included in Table I to illustrate the method of analysis. At the time these data were obtained, no 60-cycle control was taken, but the subject's mean 60-cycle let-go current was used to give an

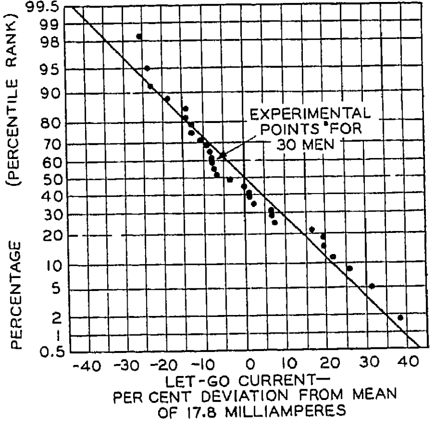

Figure 6. 180-cycle deviation curve for men

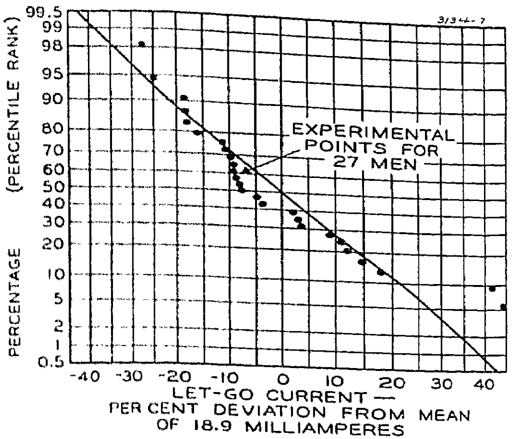

Figure 7. 500-cycle deviation curve for men

approximate correction. Computed points lie close to smooth curves, the data appear to be consistent, and the curves should represent the response for a large group of normal men. The curves read from below upwards illustrate the probability that the current they represent will be dangerous. As previously discussed, a reasonably safe current for normal male adults is proposed as that represented by the lowest curve which indicates values safe for at least 99½ per cent of the group tested.

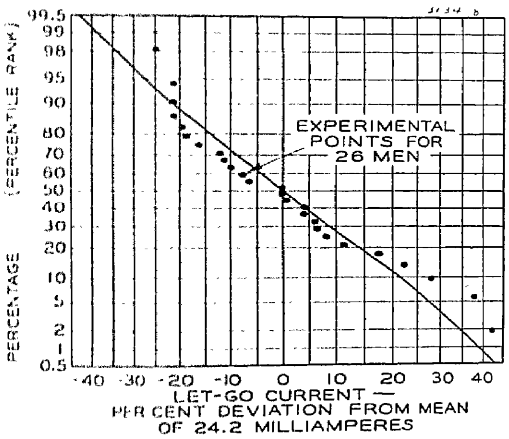

Figure 8. 1,000-cycle deviation curve for men

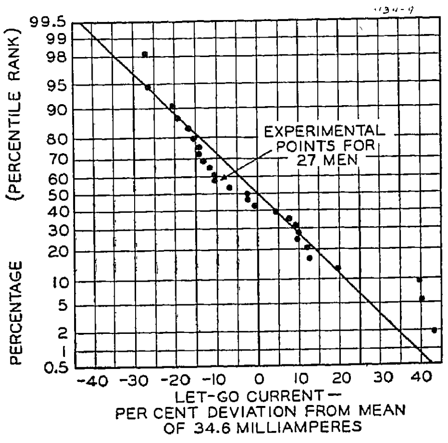

Figure 9. 2,500-cycle deviation curve for men

A safe current for normal women (Figure 5A) was computed as follows:

99½ percentile rank for 60 cycles $= 10.5\ (1-0.432) = 6.0$ milliamperes rms from equation 1

The response for normal women on any frequency or wave form is obtained from

$$\text{Percentile rank} = \frac{10.5}{15.87}\left[\begin{array}{c}\text{corrected mean of sample for}\\ \text{men on desired frequency or}\\ \text{wave form}\end{array}\right] \times (1 \pm \text{deviation from mean}) \quad (4)$$

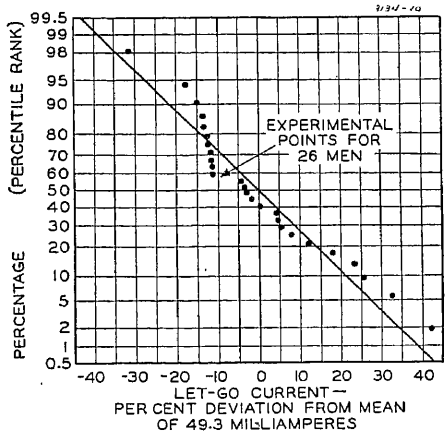

Figure 10. 5,000-cycle deviation curve for men

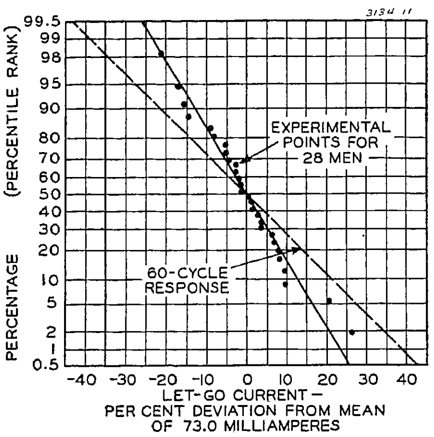

Figure 11. 10,000-cycle deviation curve for men

from which it is apparent that the let-go current for women is equal to 10.5÷15.87 = 66 per cent of the corresponding percentile values for men. Hence the safe current for normal women is equal to approximately two-thirds of the bottom curve of Figure 14. This procedure is predicated on the assumption that the physiological phenomena associated with let-go currents for both normal men and women are similar and that the ratio of the let-go currents is the same at all fre-

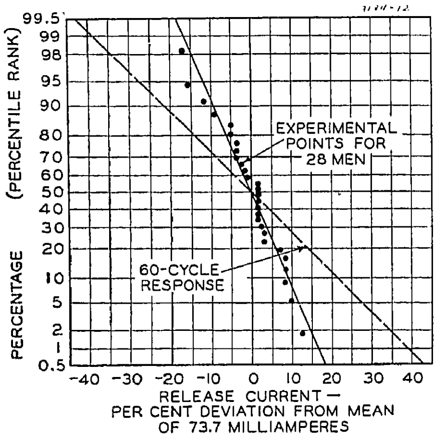

Figure 12. D-c deviation curve for men

Table I. Let-Go Currents for Men Versus Frequency

Frequency	Mean of Sample	Mean 60-Cycle for Group	Corrected Mean	Percentile Rank ½	1	25	50	75	99	99½
Deviation from mean (from Figure 5)				0.432	0.390	0.115	0.00	−0.115	−0.390	−0.432
60	15.87	15.87	15.87	22.7	22.1	17.7	15.87	14.0	9.7	9.0
5	23.4	14.56	25.5	36.5	35.5	28.4	25.5	22.6	15.5	14.5
10	16.27	14.94	17.3	24.8	24.0	19.3	17.3	15.3	10.5	9.8
25	15.14	15.11	15.9	22.7	22.1	17.7	15.9	14.1	9.7	9.1
180	17.8	15.42	18.3	26.2	25.4	20.4	18.3	16.2	11.2	10.4
500	18.9	15.53	19.3	27.6	26.8	21.5	19.3	17.1	11.8	11.0
1,000	24.2	15.84	24.2	34.7	33.7	27.0	24.2	21.5	14.8	13.7
2,500	34.6	15.57	35.2	50.4	48.9	39.3	35.2	31.2	21.5	20.0
5,000	49.3	15.17	51.6	73.9	71.7	57.8	51.6	45.7	31.5	29.3
Deviation from mean (from Figure 11)				0.260	0.234	0.068	0.00	−0.068	−0.234	−0.260
10,000	73.0	15.48	74.8	94.3	92.3	79.9	74.8	69.7	57.3	55.3
Deviation from mean (from Figure 12)				0.185	0.167	0.049	0.00	−0.049	−0.167	−0.185
D-c	73.7	15.37	76.1	90.3	88.9	79.9	76.1	72.5	63.5	62.1

quencies. It is believed that discrepancies due to sex differences are too small to affect practical conclusions with regard to electrical safety.

Sensations at the higher frequencies, especially those on 10,000 cycles or higher, were less painful, and the severity of the muscular contractions was less than that on the lower frequencies. Alarming sensations of internal heating were produced, muscular control was sluggish, and the time to let go the conductor increased considerably. These differences in physiological phenomena were associated with a deviation curve having a greater slope (see Figure 11). A straight line was drawn through the majority of the points to determine the deviation curve. The 60-cycle response was sketched on the curve for comparison. Although the experimental points fell close to the deviation curve, the accuracy of predicting the response for a large group is less than that for the lower frequencies, since it depends upon the accuracy of the small amount of data available on this frequency alone. However, the data were analyzed in a manner similar to that used for the other frequencies and are included in Table I.

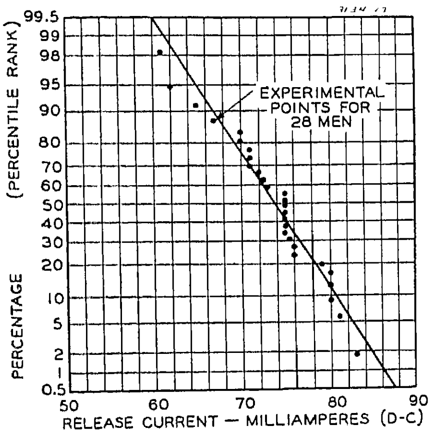

Figure 13. D-c release-current distribution curve for men

Tests on steady or gradually increasing direct currents produced sensations of internal heating rather than muscular contractions. Sudden changes in current magnitudes produced muscular contractions, and interruption of the current produced a very severe shock. The muscular reactions when the test electrode was released at the higher values were objectionable, and sooner or later all subjects declined to take more punishment. Tests were made on 28 men, and in each case little difficulty was experienced in releasing the electrode. The maximum a subject would take and release was termed the release current. It represents the limit of voluntary endurance rather than the let-go limit. The deviation curve for these tests is shown in Figure 12 and the release curve in Figure 13. At the conclusion of the 60-cycle let-go-current tests on 27 women, the experiments were terminated with one or two release tests using direct current. After one or two preliminary trials, the current was increased to a maximum of 35 milliamperes. Each subject released the electrode without complaint or difficulty. As previously reported,[1] a single woman was tested at the time the d-c tests were made on the men. She released 56 milliamperes direct current before refusing more.

The data for the d-c tests were analyzed in the same way as those for alternating currents but with less certain justification:

Corrected mean of sample $=\frac{15.87}{15.37}\times 73.7$

$=76.1$ milliamperes direct current (from equation 2)

Deviation from mean for $99^1/_2$ percentile rank $=-0.185$ (from Figure 12)

$99^1/_2$ percentile rank for men

$=76.1(1-0.185)=62.0$ milliamperes direct current (from equation 3)

$99^1/_2$ percentile rank for women

$=\frac{10.5}{15.87}\times 76.1(1-0.185)=41.0$ milliamperes direct current (from equation 4)

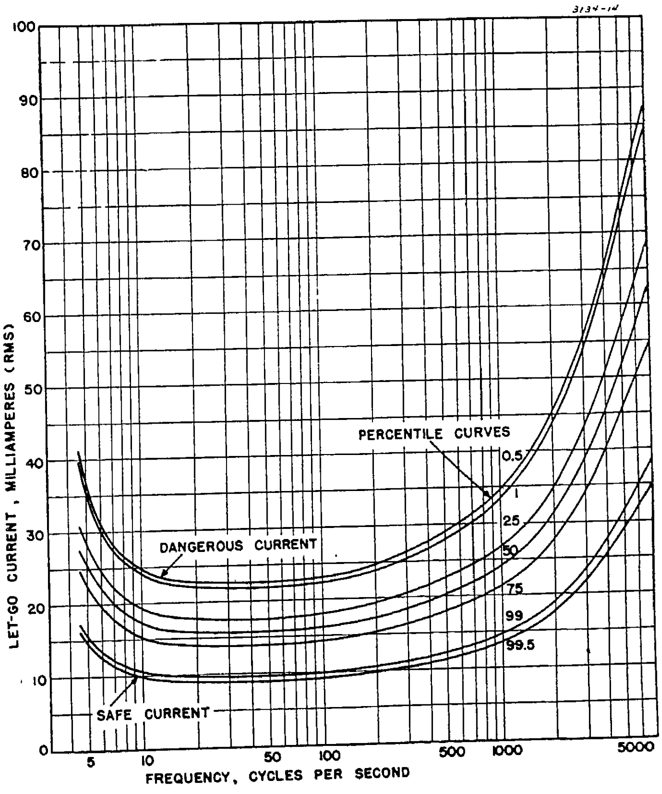

Figure 14. Sine-wave let-go current for men versus frequency

Current values become dangerous progressively to an increasing number of persons, as indicated by the percentile values on the right-hand side of the curves

Values for women are approximately 66 per cent of the current values shown on the curves

Based on the preceding data it is concluded that 62 and 41 milliamperes is a reasonably direct current for men and women respectively. Undoubtedly the maximum direct currents which are reasonably safe are in excess of these values. However, it would be unwise to suggest higher values as safe until additional experimental research is available

Electrical Safety

Although current and *not* voltage is the proper criterion of shock intensity, the danger of accidental electric shock on commercial circuits is due almost entirely to the voltage of the circuit. Rather than that the layman or the public should be confused with a technical argument, he must be warned that all power circuits are dangerous. Many deaths are caused each year from accidental contacts with ordinary 120-volt lighting circuits. Contacts with house circuits are especially hazardous in moist or wet locations.

Conclusions

1. A reasonably safe electric current for normal healthy adults is the let-go current which $99^1/_2$ per cent of a large group can release by using muscles directly affected by that current.

2. The reasonably safe 60-cycle current for most normal healthy adult men is about nine milliamperes; the reasonably safe 60-cycle current for most normal healthy adult women is about six milliamperes.

3. Let-go currents are affected by fre-

quency. Unfortunately, the power frequencies appear to be the most dangerous. Larger currents may be tolerated for both the higher and the lower frequencies and for direct current.

4. The public must be warned continually of the danger of accidental contact with power circuits and against using defective home appliances.

References

1. Electric Shock, **Charles F. Dalziel, John B. Lagen, Joe L. Thurston.** AIEE Transactions, volume 60, 1941, pages 1073–9; discussion pages 1295–7.

2. The Electric Fence, **Charles F. Dalziel, James R. Burch.** *Agricultural Engineering*, volume 22, November 1941, pages 399–406.

3. The Improved Single-Impulse Electric Fence, **Charles F. Dalziel.** *Agricultural Engineering*, volume 23, September 1942, pages 287–90.

Some Measures of Electrical-Brush Disintegration

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Synopsis: So far as can be determined, the development of the concepts of wear resistance, wear resistivity, wear susceptance, and wear susceptibility as given in this paper is thoroughly new and original. These concepts are believed to be significant and useful tools that may further an understanding of the basic problems of sliding electrical contacts. For example, they offer means for making a valid comparison of brushes and brush materials insofar as wearing qualities are concerned. Moreover, they provide a clue to the proper consideration and evaluation of the large number of variables that are at work as brush disintegration takes place.

THE subject of wear itself is beset by many complications arising from the fact that the subject, at best, is largely statistical. It is accepted generally among authorities that there is no such thing as a perfect sliding contact, in which all sliding phenomena are continuous and uniform. Only grossly macroscopic data appear to be continuous, while data approaching microscopic proportions reveal discontinuities that are more or less periodic. It is conceded generally that a sliding contact consists of a number of minute point contacts, whose behavior is extremely individualistic. All of the characteristics of the macroscopic contact originate at these minute points; and it is logical, therefore, to infer that extreme conditions, such as excessive pressures, excessive current densities, excessive temperatures, and others, exist at these points. It is exceedingly difficult, therefore, to obtain a true picture of a sliding contact by means of the macroscopic data that are available to most experimenters and engineers. This paper is intended as an aid in the efforts to study sliding-contact materials by means of these macroscopic data.

Further complications arise when the term *wear* is applied to the undesirable destruction of all surfaces, whatever the manner in which the destruction may occur. The wear of shoe soles and heels, the wear of piston rings and cylinder walls, the wear of bearings, the wear of cutting edges, and many other types of wear, each involves a peculiar and, perhaps, unique combination of forces that provide the mechanism of wear. Therefore, when discussing wear, it is necessary to specify the particular kind that is being considered as well as the multitude of conditions that influence it.

This paper is intended to include only the type of wear that is encountered in the use of continuously sliding contacts, particularly the end wear of brushes riding on collector rings. Even in this simple case, however, it is impossible to comprehend the true mechanism of the wear of a sliding contact in all its details. As evidence of the great complexities that exist in a sliding contact, an abbreviated chart of the known variables that enter into the phenomenon of the wear of electrical brushes is shown in Figure 1. It is beyond the scope of this paper to discuss the part that each variable has in the whole process of brush wear, but it is not difficult to conceive by means of this chart the truth that the whole process is highly involved and cannot be disentangled very easily, if at all.

Comparative

Nevertheless, it is desirable that electrical brushes be compared on the basis of relative wearing qualities as well as on the bases of contact resistance, current-carrying capacity, and other performance characteristics. V. P. Hessler has made a successful attempt within limitations to study the effects of various operating conditions upon electrical-brush wear as well as on brush contact drop.[1–3] He considered chiefly the effects of current density, ring speed, brush pressure, and relative humidity. That material has been distinctly useful in advancing toward a better understanding of basic relationships.

However, further progress could be made if a concept of wearing quality were developed that would apply strictly to the brush and not include the properties of the external system of which the brush is a part. No fair comparison can be made of brushes at the present time without testing all of them on the same test equipment and under identical conditions. Since many different grades of brushes are made for use under widely different conditions, testing them under like conditions is not always useful. Even though they be tested under like conditions, there is yet the problem of distinguishing between the effect of the conditions imposed on the interface by the whole sliding system, including the brush, and that of the structural properties of the contact faces. It is the intended purpose of this paper to develop a means whereby this distinction between the conditions responsible for producing wear and those responsible for resisting wear can be made. The latter conditions shall be implied in this paper by the term *wear resistance.*

The wear of a contact face is caused by the frictional forces that are present when sliding occurs. The structural characteristics of the contact specimen determine the resistance of the latter to wear when sliding occurs. Thus, the numerous variables of wear as evidenced by Figure 1 may be classified into two important groups—those which affect the frictional forces in one way or another, and those which affect the wear resistance of the contact specimen. The first group is associated with the whole sliding system while the second group concerns only the contact specimen itself. Of course, a particular variable may belong in both groups, as, for example, brush current. It is the means for measuring the effects of the second group on wear that are to be considered.

There is a philosophical truism that

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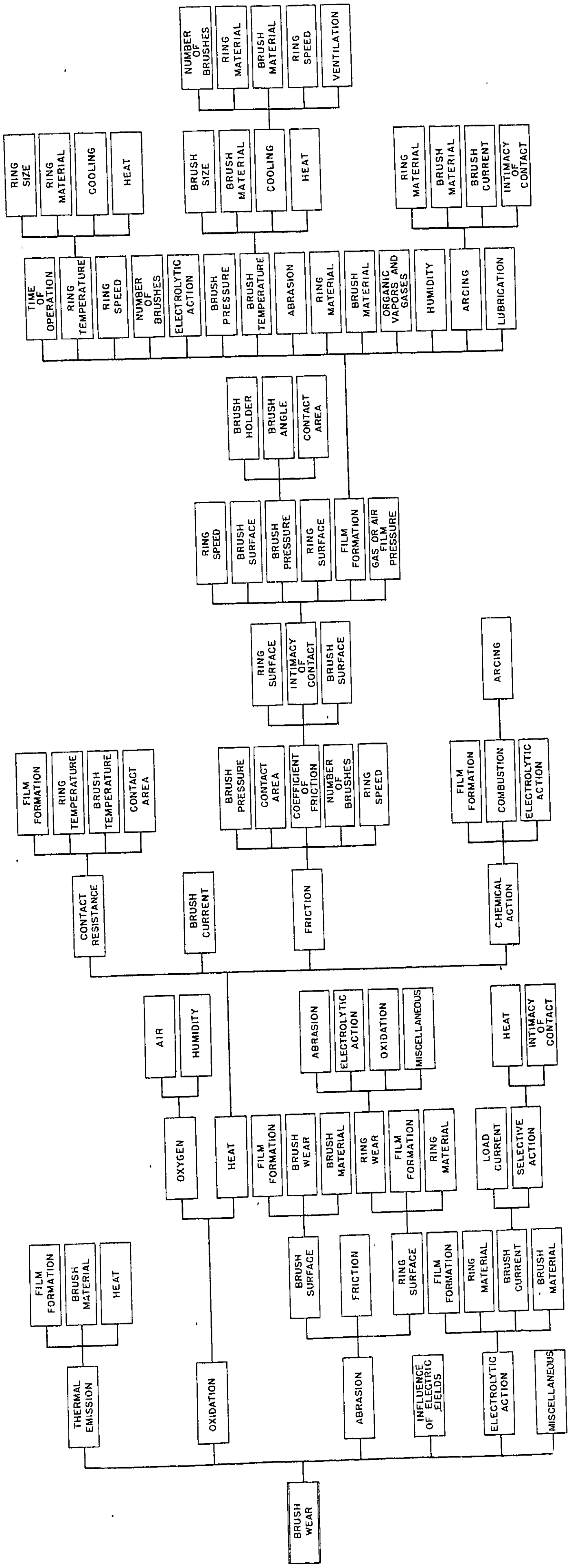

Figure 1. Elements of electrical-brush wear

asserts that the magnitude of any phenomenon is directly proportional to the magnitude of its cause and inversely proportional to the magnitude of that which resists or impedes the occurrence of the phemonenon. This truism is the basis for Ohm's law in the electrical system and for the analogous relationships in the magnetic and mechanical systems, respectively. It is proposed that it be applied to the phenomenon of wear. Since frictional force is the direct cause of wear, and the amount of wear is best measured as a time rate, it is suggested that that property whereby wear is resisted be defined so that the following relation will hold:

$$\text{Rate of wear} = \frac{\text{frictional force}}{\text{wear resistance}} \qquad (1)$$

It is believed that this equation represents a relationship that is just as fundamental and just as basic as is Ohm's law. In the light of this belief the efforts found in the literature to date to define wear resistance will be considered next.

It is interesting to note that the subject of wear has been studied from many different viewpoints, particularly in the field of metallurgy, and that many ingenious machines and other devices have been designed and built to test the wearing qualities of a variety of materials under controlled conditions that were intended to simulate actual working conditions. Only two papers have been found in the literature to date in which an attempt is made to define wear resistance by a mathematical expression. Neither of these expressions excludes wholly the properties of the external system. The first expression is defined by R. B. Freeman[4] as "unit wear," which he set equal to the expression obtained by "dividing the loss of weight experienced by the specimen during the run by the projected area of contact and the length of travel." This unit wear is measured in grams per square inch per foot and is expressed dimensionally as $ML^{-2}T^{-2}$. The chief disadvantage to this concept of unit wear is that it is a function of the density of the contact material being studied. Thus, if two specimens of differing densities should be tested under identical conditions and found to have the same volume of wear, the preceding expression would show the heavier specimen as having greater unit wear. It might be suggested that this definition be revised by substituting the loss of volume for the loss of weight. Such a ratio would then be equivalent to the ratio of length of end wear to the relative distance traveled by the contact faces and would be dimensionless. Both definitions, however, are functions of the coefficient of friction and, therefore, do not provide a true comparison between different specimens for even the same contact area and contact travel. Thus, if one brush wears twice as much as another for the same ring travel and brush size, its unit of wear would be double that of the other, regardless of unequal surface conditions and other unlike conditions that influence the coefficient of friction.

The second expression that was found for wearing quality is defined by D. S. Clark and R. B. Freeman[5] as the energy required to overcome resistance to motion per unit volume of wear and is expressed dimensionally as $ML^{-1}T^{-2}$. The energy required to overcome resistance to motion, however, is a function of the coefficient of sliding friction as well as other properties that depend on the whole sliding system. The concepts of resistance to motion and of the energy required to overcome this resistance are treated later in this paper.

Table I

Electrical		Mechanical		Wear	
Inductance	L	Mass	M	Mass	M
Charge	Q	Length	L	Length	L
Time	T	Time	T	Time	T
Current	QT^{-1}	Velocity	LT^{-1}	Wear rate	LT^{-1}
Voltage	LQT^{-2}	Force	MLT^{-2}	Wear force	MLT^{-2}
Resistance	LT^{-1}	Resistance to motion	MT^{-1}	Wear resistance	MT^{-1}

Analytical

The frictional forces available for measurement are the tangential and normal forces, respectively, and the work done by (or against) each is the product of that force by the component of the relative motion of the contact member in the same (or opposite) direction of that force. Whenever force is applied to produce relative motion, the discrepancy between the applied force and the reactive forces due to the acceleration of the body is determined by frictional forces that produce heat and wear.

This statement may be represented by the following equation:

$$F=m\frac{dv}{dt}+K_dv \tag{2}$$

where F is the applied force, m is the mass of the system to which the force is applied, v is the velocity of the system, and K_d is the resistance to motion.

This may be illustrated by the motion of a streetcar on tangent level track during the accelerating period with constant torque output from the motor. The difference between the force applied and the summation of the frictional forces is the net force that produces acceleration.

The normal and tangential forces involved in sliding friction contribute mechanical energy to both wear and heat in unknown and quite variable proportions. The total energy delivered to the sliding contact is equal to the integral of the product of the average tangential force times the differential of the tangential distance traveled and of the product of the normal force times the differential of the depth of the wear or distance of cut, measured in the direction of the normal force. For ordinary sliding contacts where gouging is absent, the second product is negligible, while for the action of cutting tools the first product is negligible.

If resistance to motion be defined as

$$\text{Resistance to motion} = \frac{\text{frictional force opposing motion}}{\text{rate of motion}} \tag{3}$$

(or simplified this becomes $K_d=F/v$), then its dimensions are MT^{-1}, and the energy needed to overcome this resistance to motion may be defined as follows:

$$\int(\text{frictional force opposing motion})(\text{rate of motion})dt=\int(\text{rate of motion})^2(\text{resistance to motion})dt \tag{4}$$

Note the similarity of equations 3 and 4 to the electrical equations $R=E/I$ and energy $=\int EIdt=\int I^2Rdt$. Since the process of wear is brought about both by weakening the structure of the contact face to an unknown extent and by overcoming through sheer force an unknown part of the remaining strength of this structure, it is impossible to set up an exact expression for the energy absorbed by wear. To state it in another way, wear energy consists of heat, mechanical, and chemical energies and so far is beyond the reach of the experimenter's measuring devices. The definition of wearing quality given by Clark and Freeman may now be expressed as

$$\frac{\int(\text{frictional force opposing motion})(\text{rate of motion})dt}{\int(\text{projected area of contact})(\text{rate of end wear})dt}$$

Before wear resistance can be defined more explicitly than is shown in equation 1, it is necessary to determine what shall be meant by rate of wear. Rate of end wear can be measured in three ways:

1. Rate of loss of weight.
2. Rate of loss of volume.
3. Rate of loss of length (measured normal to the contact face).

It has been shown already that the rate of loss of weight is a function of the density of the contact material and is, therefore, unsuitable as the basis of comparison between unlike materials. The choice between rate of loss of volume and rate of loss of normal length can be made more easily by comparing two hypothetical contacts of the same homogeneous material, one of them having twice the contact area of the other. Reference to Figure 2 should assist with a visualization of how the hypothetical contacts may be expected to function.

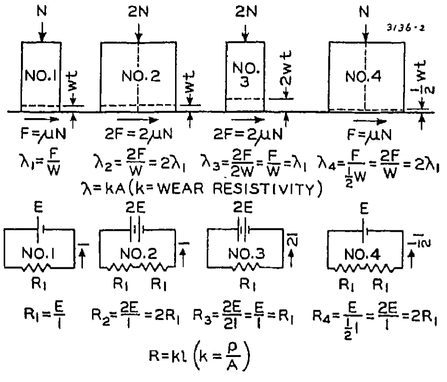
N
2N
2N
N
3136-2
NO.1
NO.2
NO.3
NO.4
wt
wt
2wt
$\frac{1}{2}$wt
F=μN
2F=2μN
2F=2μN
F=μN
$\lambda_1=\frac{F}{W}$
$\lambda_2=\frac{2F}{W}=2\lambda_1$
$\lambda_3=\frac{2F}{2W}=\frac{F}{W}=\lambda_1$
$\lambda_4=\frac{F}{\frac{1}{2}W}=\frac{2F}{W}=2\lambda_1$
λ=kA(k=WEAR RESISTIVITY)
E
2E
2E
E
$\bar{I}$
$\bar{I}$
$2\bar{I}$
$\frac{\bar{I}}{2}$
R1
R1 R1
R1
R1 R1
$R_1=\frac{E}{I}$
$R_2=\frac{2E}{I}=2R_1$
$R_3=\frac{2E}{2I}=\frac{E}{I}=R_1$
$R_4=\frac{E}{\frac{1}{2}I}=\frac{2E}{I}=2R_1$
$R=kl\left(k=\frac{\rho}{A}\right)$

Figure 2. Analogy between wear and electrical resistances

It may be helpful at this point to compare the concept of wear resistance to that of electrical resistance. First of all, the R of Ohm's law ($I=E/R$) is a function of both the properties of the material and the dimensions of the electrical specimen. Secondly, that property of resistance to electrical conduction which is characteristic of the material alone is called resistivity and is related to R by means of the dimensions of the specimen. In like manner, it is desirable that wear resistance be dependent solely on the characteristics of the contact specimen, while *wear resistivity*, another new concept of wearing quality, be dependent solely on the properties of the contact material. The striking similarity between wear resistance and electrical resistance is shown by juxtaposition of their corresponding systems in Figure 2. It should be pointed out that this analogy between sliding contacts and electrical circuits has the same limitation that all analogies have—that it can be carried too far, yielding some absurd results.

The requirement of identical conditions for these two hypothetical contacts implies that both of them must have the same normal pressure (say, pounds per square inch) and, hence, that the larger contact have twice the normal force of the smaller. Since both contacts are of the same material, their coefficients of friction (μ) may be assumed equal; therefore, the tangential force of the larger contact will be twice that of the smaller contact. Since the larger contact may be considered as two contacts like the smaller one fastened together, it may be assumed that both hypothetical contacts will have the same depth of end wear. Accordingly, the larger contact will lose twice as much volume as the smaller one. The limitation of this assumption will be treated later.

If the wear resistance be defined as the ratio of the tangential force to the rate of loss of normal length, the larger contact will appear to have twice the resistance of the smaller one. If, however, the wear resistance be defined as the ratio of the tangential force to the rate of loss of volume, both contacts will be indicated as having the same wear resistance. The first definition is a function of the properties of the whole contact specimen, while the second is a function of the properties of the contact material. Therefore, it has been chosen to call the ratio of the tangential force (F) to the rate of loss of normal length (W) the wear resistance (λ), whose dimensions are now MT^{-1}. Likewise, wear resistivity (k) is to be defined as the ratio of the tangential force to the rate of loss of volume (WA) and is expressed dimensionally as $ML^{-2}T^{-1}$. Therefore, it follows that

$$\text{Wear resistance} = (\text{wear resistivity})\ (\text{contact area}) \quad (5)$$

This can be clarified readily by reference to the four cases illustrated in Figure 2, in which the expressions for wear resistance are given. The expressions for wear resistivity for these four cases are, respectively, as follows:

$$k_1=\frac{F}{WA}=\frac{\lambda_1}{A}$$

$$k_2=\frac{2F}{W\cdot 2A}=k_1=\frac{\lambda_2}{2A}$$

$$k_3=\frac{2F}{2W\cdot A}=k_1=\frac{\lambda_3}{A}$$

$$k_4=\frac{F}{(W/2)2A}=k_1=\frac{\lambda_4}{2A}$$

The analogy to a parallel-resistance electric circuit is a multiple sliding-contact system and will not be treated in this paper.

It is proper at this point to consider the limitations of the hypothetical contact when applied to actual cases. It being assumed that the foregoing developments are logical, it is considered perfectly valid to let these concepts of wear resistance and wear resistivity absorb all of the variations in the physical properties of the contact member just as electrical resistance and electrical resistivity do for the case of electric conduction through any conductor. Thus, just as R under operating conditions is equal to

$$\frac{\rho_0 l}{A}\times K(\text{temperature})\times K(\text{stranding})\times K(\text{skin effect})\times K(\text{etc.})$$

so wear resistance may be set equal to

$$(\text{Wear resistivity})_0\times(\text{contact area})\times K(\text{contact temperature})\times K(\text{contact dimensions})\times K(\text{contact pressure})\times K(\text{sliding system})\times K(\text{ambient conditions})\times K(\text{etc.})$$

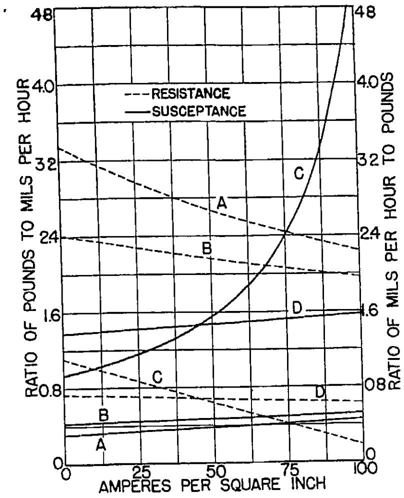

Figure 3. Wear resistance and wear susceptance versus current density for metal-graphite brushes—types *A*, *B*, *C*, and *D*

It should be pointed out that the adaptation coefficients for wear resistance as already given are not as simple as are those for the electric circuit. In the latter case these coefficients are both fairly simple and quite dependable. For wear phenomena, however, these coefficients are extremely variable and interdependent. A study of Figure 1 will verify this statement to a large extent.

The reciprocal of wear resistance, which may be called *wear susceptance*, has been found, in the case of electrical brushes, to be more easily understood than wear resistance. Since wear is the result of friction, it is more intelligible to think of the rate of wear per unit of friction than of the amount of friction per unit of wear rate. Likewise, the reciprocal of wear resistivity, which may be called *wear susceptibility*, is more useful than is wear resistivity.

Experimental

The curves shown in Figures 3 and 4 for four kinds of metal-graphite brushes were calculated from experimental data obtained at the Iowa Engineering Experiment Station and represent the average of repeated tests at each current density between 0 and 100 amperes per square inch at intervals of 20 each. The current used was 60-cycle alternating current. The measured data included frictional torque of the test brushes, their wear, and the spot temperature in a representative brush of each group tested. The wear is measured in mils and the wear rate in mils per hour.

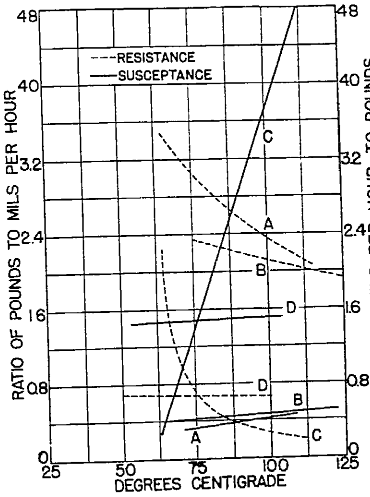

Figure 4. Wear resistance and wear susceptance versus spot temperature for metal-graphite brushes—types *A*, *B*, *C*, and *D*

The test equipment consisted of two units of similar construction, each having a slip ring of standard material driven by a separate motor. Each slip ring, together with its brushes, was enclosed so as to permit the control of atmospheric conditions. Eight brushes were placed radially on each ring with a normal pressure of three pounds per square inch of contact area and were tested at an average speed of 4,200 feet per minute.

The duration of each test run was the same, and data were recorded at regular intervals throughout each run. All values except brush-wear data used in the calculations of the curves of Figures 3 and 4 represent the averages of these recorded data for a set of runs at each current density except for that obtained during the first hour of each run. This first hour's data were influenced by transient conditions that accompanied the start of every test run, but the rest of the data for each run were, for the most part, steady state. Both test units, which had previously been compared and checked for similarity of results on like brushes, were operated simultaneously in all runs. The brushes were tested as follows:

Types *A* and *B* simultaneously on units 1 and 2, respectively.

Types *C* and *D* simultaneously on units 1 and 2, respectively.

Each series of tests was preceded by a process of stoning and sanding followed by an operating period of sufficient length to wear in the brushes properly and establish a well-developed characteristic ring film. The film was henceforth undisturbed until the series of runs had been completed, and it was necessary to make a similar preparation for the next set of brushes. Experience has shown that the film formation on a ring is a function of the brush current as well as of other operating conditions and that large changes of current density produce results that are not characteristic of either the original or the final current density. It was found, however, that an increase of current density in steps of 20 amperes per square inch each permitted tests that were wholly reliable within the limits of experimental error. Several runs were conducted at each current density before proceeding to the next.

The frictional force, which is measured in pounds, was obtained by converting the electrical input to the driving motor into the torque required by the brushes. The efficiency data required for these conversions were revised for every test run, so as to prevent, insofar as possible, the errors due to the variable losses of the test equipment.

The brush-wear data were obtained by making four micrometer measurements, each to the nearest 0.0001 inch, on each of the eight brushes, before and after each run. The difference between successive readings for each of the 32 points helped to determine the average brush wear for that set of brushes.

The spot temperature was measured by a thermocouple junction placed in a hole drilled in the top of one brush in the group being tested. It has been found that consistent use of the same point in all tests gives approximately the same comparative results as a group of thermocouples placed at various accessible points on a brush. This temperature, which is plotted as abscissa in Figure 4, does not, however, represent the true temperature of the contact face. There are three important temperature ranges in a sliding contact member—the body temperature, the face temperature, and the local spot temperatures at the points of most intimate contact. Each of these ranges is exceedingly variable, both in intensity and extent. It is a truly difficult problem to obtain a true picture of the temperature conditions in the contact and its members. Nevertheless, it is believed that the curves shown in Figure 4 are not totally devoid of significance and that they may be used for rough but very useful comparison.

The values of wear resistance and wear susceptance are plotted against current density for practical reasons and against temperature for theoretical reasons. Practically, the similarity of the curves of wear susceptance and wear resistance in Figure 3 to those obtained for rate of brush wear and its reciprocal, brush life, respectively, indicates that friction alone was not responsible for the variations in wear rates. The phenomenon of wear is a function of both its cause, which is friction, and the binding strength of the material, which restrains it to a varying degree. The binding strength is, in turn, a function of the temperature of the brush material, weakening as the temperature increases. Since the brush temperature increases with an increase in current density, it is reasonable to expect the ratio of the wear to the friction to increase. likewise.

Theoretically, the curves of Figure 4 indicate the relative strength or weakness of the binding materials of the respective brushes for similar ranges of temperature. It is easy to recognize the superiority of the binding materials in brushes *A* and *B* and the great inferiority of the binder in brush *C*. It should be noted again that this ratio of wear to friction is almost completely independent of the external system and is almost wholly a property of the brush itself.

Conclusions

The development of concepts of wearing quality based on the truism that the magnitude of a phenomenon is proportional to its cause and inversely proportional to that which resists it has been given, and two of these concepts are shown for the actual cases of four kinds of metal-graphite brushes. Since electrical resistance, resistance to motion, and wear resistance are based on the above truism, it is no surprise that electrical, mechanical, and wear phenomena should be analogous to some extent. The table shown below illustrates the extent of the analogy for all three phenomena insofar as can be determined at present.

It is believed that these concepts, together with the chart of variables affecting brush wear shown herein, provide a more solid foundation for the furtherance of the study of wear of electrical brushes. It is recognized that a subject so inherently complex as an electrical sliding contact cannot be made simple. It is possible, however, to analyze the total phenomenon into its component parts and, where these component parts are also exceedingly complex, to analyze them further into simpler components, repeating the process until components that can be determined readily are obtained. The first step in this process of analysis has been suggested in this paper by the separation of the characteristics of the contact specimen itself from those of the outside system.

Apart from abstract analysis, however, wear resistance, wear susceptance, wear resistivity, and wear susceptibility are believed to provide new and important tools for the comparison of brushes or other materials used in sliding contacts.

References

1. THE EFFECT OF VARIOUS OPERATING CONDITIONS UPON ELECTRICAL BRUSH WEAR AND CONTACT DROP, **V. P. Hessler.** *Bulletin* 122, Iowa Engineering Experiment Station, Iowa State College, November 1935.

2. ELECTRICAL-BRUSH WEAR, **V. P. Hessler.** AIEE TRANSACTIONS, volume 54, 1935, October section, pages 1050–4.

3. ABRASION—A FACTOR IN ELECTRICAL-BRUSH WEAR, **V. P. Hessler.** AIEE TRANSACTIONS, volume 56, 1937, January section, pages 8–12, 16.

4. THE RELATIVE WEAR OF SOME HARD METALS, **R. B. Freeman.** *Metal Progress*, volume 31, 1937, pages 281–4.

5. A NEW TEST FOR RELATIVE SLIDING WEAR, **D. S. Clark, R. B. Freeman.** *Metal. Progress*, volume 31, 1937, pages 399–401.

Parallel Operation of Airplane Alternators

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Synopsis: The increase in airplane electrical loads has made necessary an increase in the distribution voltage to avoid excessive copper weight. Difficulties in the use of higher d-c voltages are mentioned, and past experience with a 400-cycle three-phase system is outlined. In the light of this experience and of new developments in progress, requirements are outlined for a larger 400-cycle system with multiple generating units operating in parallel.

ELECTRICAL loads on military airplanes have increased tremendously in the last three years because of the substitution of electric power for hydraulic power to drive auxiliaries, or the use of motor-driven instead of engine-driven hydraulic pumps, and because of the development of new flight and combat devices.

The Trend Toward Higher Voltage

First in use because of its simplicity, the d-c system progressed through the 6- and 12-volt stages until, in 1939, new airplanes were designed with a 24-volt system.[1] As the loads increased, the weight of wiring required for the lower-voltage systems became prohibitive. Now, in 1943, the economical limit of the 24-volt system has been exceeded, with new airplanes demanding 1,600- to 2,000-ampere peak loads. A further increase in voltage is therefore necessary. Because of the continued upward trend of the load-demand curve, and because of the increasing transmission distances in the larger airplanes, the next step will be above 100 volts.

Unfortunately a change to 115 volts direct current on high-altitude airplanes introduces at least one problem to which a completely satisfactory answer has not yet been found. This is the difficulty of arc rupturing in the switching equipment at present and contemplated flight-altitude maxima for combat airplanes. Fault currents may be higher than for a-c equipment of equivalent rating. On small motors at this voltage, commutation at high altitudes is another problem. Partly for these reasons, the United States Army Air Force has selected a 208/120-volt 400-cycle three-phase four-wire system for the large combat airplanes now under development.

The XB-19 Installation

Early experiments by the Air Corps with a-c auxiliary power on the *XB*-15 resulted in procurement of auxiliary-engine-driven 400-cycle three-phase power plants for the *XB*-19. Figure 1 is a schematic view showing the circuit arrangement. The design of the engine and alternator was co-ordinated to result in the package unit shown in Figure 2. The pancake proportions of the alternator do not give the optimum weight of electrical parts, but, in conjunction with the aluminum fan, provide the necessary flywheel effect for the engine. The use of 14 poles gives a synchronous speed of 3,430 rpm. The short-circuit ratio is 0.60, a figure which may seem low to a designer of 60-cycle machines, but which has proved satisfactory in operation. Heavy dampers with continuous end rings limit oscillation caused by engine-torque pulsation. They also help support the voltage until the regulator responds to a load increment. With a self-excited system, this materially assists in prompt recovery of voltage if the regulator is fast enough to take advantage of it.

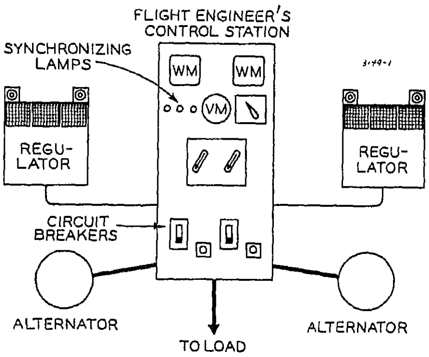

Figure 1. Schematic view of the auxiliary power plants used on the *XB*-19 airplane

The design rating of the alternator is 120 volts, 12.5 kva, 0.8 power factor, although, to conform to the Air Corps specification, the name-plate rating is 10 kva, 0.75 power factor. As the system is operated ungrounded, the neutral is not brought out.

The rotating field of the alternator is designed for overhung mounting on the engine crankshaft, which makes the use of a d-c exciter difficult. Chiefly for this reason, it was decided to use self-excitation. An electronic voltage regulator, using *RO*-585 diodes to control a three-phase half-wave thyratron rectifier, provides the fast-acting field supply so necessary in a self-excited system.

The *RO*-585 filaments are operated from a positive-phase-sequence network which delivers a single-phase voltage proportional to the average of the root-mean-square values of the three line voltages. As the regulating action is a function of the filament temperature in these tubes, the regulator is responsive to the average of the three root-mean-square phase voltages. An incidental advantage in using the positive-sequence network is the accomplishment of excellent temperature compensation by using in the resistance element of the network a material having a positive temperature coefficient of resistance.[2]

The flight engineer's panel, the installation of which is shown in Figure 3, contains the instrumentation and manual controls. The panel compartment houses the voltage build-up and reactive-load-equalizer circuits, the wattmeter current transformers, and the manual circuit breakers. Voltage is built up initially from an alternator residual voltage of approximately nine volts by means of a copper-oxide rectifier and a small multicontact Silverstat voltage regulator, both of which are intermittently rated to supply no-load excitation. A small high-reactance step-up transformer overcomes the initially high resistance of the rectifier without resulting in excessive excitation at normal voltage.[3] By this means the voltage overswing is reduced. For each alternator a three-position multicontact manual cam switch controls the starting sequence. This switch must be in the off position before the auxiliary-engine-starter circuit can be completed. With the engine at normal speed, the switch may be turned to the warm-up position, which allows the voltage to build up and be maintained under the control of the auxiliary regulator. After 45 seconds a thermal relay energizes a tripping magnet on the cam switch, automatically advancing the switch to the run position, although it may be returned to the off position at any time. The two cam switches are mutually interlocked to prevent an attempt to build up both alter-

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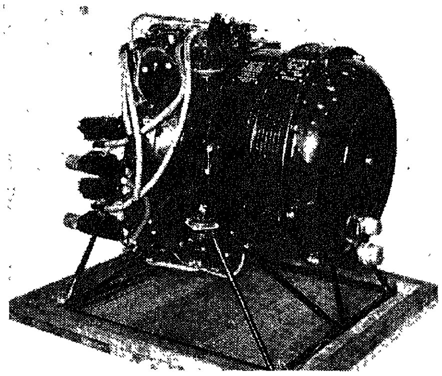


Figure 2. The 12.5-kva alternator assembled on the Ruckstell-Burkhardt auxiliary engine

nators at the same time, as only one build-up circuit is provided. As soon as one alternator is transferred to electronic regulation, it may be connected to the load bus by closing its manual circuit breaker, although, particularly at low ambient temperature, it is desirable to operate at no load for a few minutes to allow the thyratrons to reach normal operating temperature.

Operating Experience on the XB-19

With no load on the first alternator, and with the governors of the two engines closely matched, it has been found fairly easy to synchronize manually at full voltage on every attempt when the synchronizing lamps are flashing not faster than three cycles per second. At rates of slip up to six or eight cycles per second, it is possible to synchronize on approximately 75 per cent of the attempts with random closure of the breaker. With more than two or three kilowatts of load on one alternator, it is necessary to close while the lamps are dim or dark. Test experience with these power plants indicated the necessity of manual control of the engine governors, and this feature was added when the equipment was installed in the airplane. After some practice, and with the engines in smooth running condition, it was then possible for the operator to synchronize with full load on one alternator, although he would occasionally miss. When the engines have been run for a number of hours after an overhaul, momentary speed changes during attempts to synchronize make it difficult to do so with more than six kilowatts of load.

During the development stage, tests were made to determine if it were practical to synchronize by bringing the incoming alternator up to approximately synchronous speed with zero or weak field and allowing it to pull into step by means of its damper winding and reluctance torque. This attempt was not successful, because the highly reactive load severely overloaded the first alternator. While it is true that better results would have been obtained with separately excited machines, the shock would still be prohibitive when attempting to synchronize under load.

When paralleled units are driven by auxiliary engines, it is necessary to cut off promptly an alternator whose engine has failed to carry load. Magneto trouble, fouled plugs, and vapor lock, or other fuel-system faults, will occasionally cause a short-time or sustained power failure. Any tripping means for isolating such a unit should have its action delayed sufficiently to prevent operation on a momentary misfire which can be sustained by the inertia of the machine. On this equipment, protection is afforded by a reverse-power contact on the wattmeter which actuates a shunt trip magnet on the circuit breaker. This contact is set at three kilowatts, and the damping of the instrument gives the necessary delay. A small pilot relay inside the instrument case relieves the pointer contact of the tripping current.

Experience with the system has demonstrated the desirability of a uniform response rate of the several governors. If the rates differ materially, one engine tries to drive the other on the sudden removal of a large load, and faulty tripping of the reverse-power device may result. A particularly bad performer is a governor which has sluggish response in correcting overspeed. Although the condition was reproduced during preliminary testing, no serious trouble has been experienced in service operation.

Experience with the scheme for automatically building up the alternator from its residual voltage has been generally good but subject to occasional failure caused by a reduction of normal residual flux. This usually follows a shutdown caused by loss of voltage resulting from a highly inductive overload, and may occur after an engine failure has transferred all

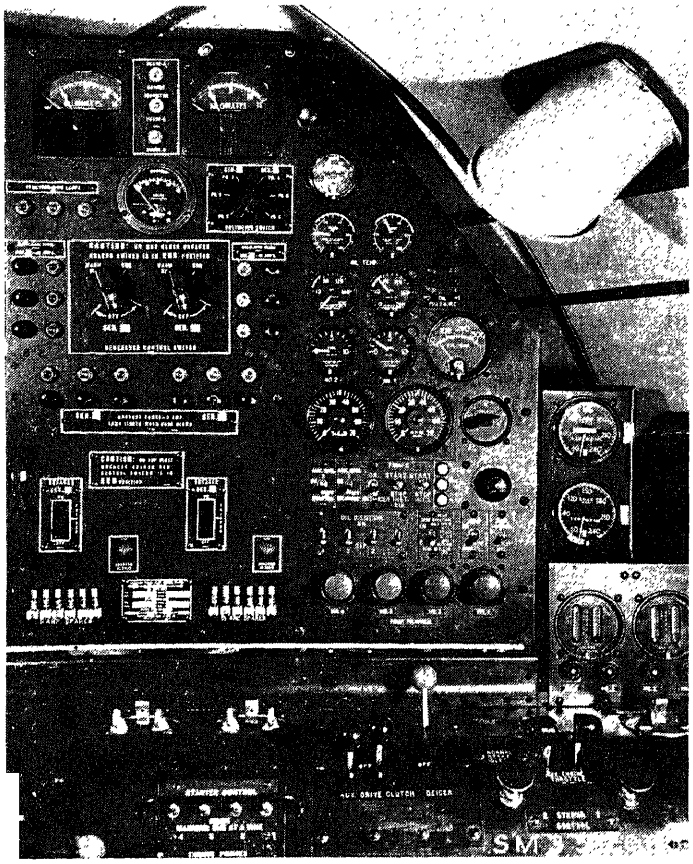


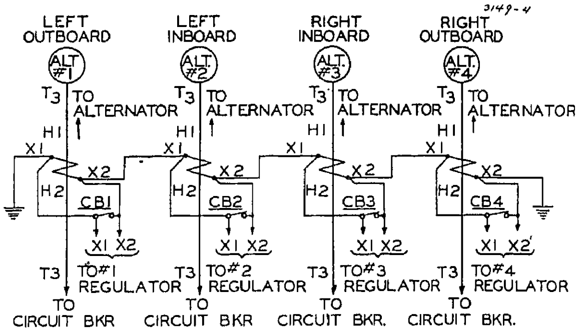
Figure 3 (left). Alternator and auxiliary-engine instruments and manual controls at the flight engineer's position of the *XB*-19 airplane

The two plunger knobs in the lower right-hand corner of the picture permit manual control of engine speed while synchronizing

Photo courtesy of Douglas Aircraft Company

Figure 4 (below). Standardized current-transformer interconnections for feeding the reactive load-equalizer networks of the voltage regulators

Agreement on polarities is necessary to permit interchanging regulators of different manufacture



of the load to the remaining alternator. It may also be discovered after the flight engineer has mistakenly shut down an engine without first opening the alternator circuit breaker. To overcome this condition, a circuit has been added which permits the operator to "flash" the field with battery voltage if necessary.

Requirements for a Modern A-C Installation

Having examined an existing a-c installation, we may proceed to lay down the requirements of an airplane power system which will take advantage of the experience gained and the newer developments which are available. It cannot well be denied that a successful system must involve parallel operation of a number of generating units. It is not acceptable that the loss of a single unit can shut down the whole system. The loss of one unit out of two is nearly as bad, unless the essential load is within the rating of one generating unit. This condition might exist in a noncombat airplane, but the greater hazards of combat operation dictate the use of at least three and preferably four units where the size of the airplane permits. Nonparalleled or isolated operation of the several generating units is a makeshift which does not use to the full the power capabilities of the system in starting heavy motor loads and self-clearing of faults; that is, the load factor with separate units is poor. Loss of a generating unit results in a delay before the load can be reapportioned among the other units.

The ability to parallel alternators depends first on the characteristics of the prime movers. The earlier a-c aircraft systems used auxiliary gasoline engines governed to run at essentially constant speed. The governors are designed to droop the speed slightly as load increases, which characteristic automatically gives acceptable division of kilowatt load between units. As the capacity of the prime mover is comparable to that of the alternator, and the inertia is relatively low, no difficulty has been experienced in maintaining synchronism under all conditions, including load transients. In the *XB*-19 equipment, the use of low-resistance damper windings effectively prevents hunting caused by engine-torque pulsations. Whereas the use of auxiliary engine drive may be satisfactory for medium-altitude transport and cargo airplanes, at the present time it seems to be impractical to supercharge the smaller engines to a critical altitude sufficiently high to permit their use on high-altitude bombers.

Constant-Speed Drives

Until recently, no suitable means was available for driving alternators from the main engines and at the same time permitting parallel operation. Now several so-called "constant-speed" drives are under intensive development and promise to have the necessary performance. The input–output speed ratio is continuously variable, so that a constant output speed may be maintained over a wide range of input speed. This development gives the a-c electric-system prime movers of proved reliability and high-altitude performance.

A number of difficulties must be overcome in adapting constant-speed drives to this application. Because of the large size of the main engine in proportion to the rating of the alternator, the engine is a rock-steady source whose speed is practically unaffected by alternator loading. On the other hand the engine speed may suddenly be changed because of flight requirements; yet the alternator should not perceive this change. As a result, the drive or its governor must incorporate the load-dividing feature, and the governor must be quick in coming into action and be able to change the drive ratio at a rate faster than the engine can accelerate. In a speed-converting mechanism the difference between the input and output torques appears as a torque reaction on the housing, and it appears to be unavoidable that this reaction torque is transmitted principally through the ratio-changing mechanism. Consequently at high ratios the ratio-changing device must develop considerable power to alter the ratio.

The necessary rate of change of ratio is determined by the maximum possible rate of acceleration or deceleration of the engine. Information from one of the engine manufacturers states that an acceleration rate of 3,000 rpm per second may be found in normal operation, although it is considered detrimental to engine life, and that a rate of 3,500 rpm per second is possible under some combat conditions with hand control of the propeller pitch. The drive should be capable of responding at a rate somewhat faster than this to have a reasonable factor of safety. It seems probable that the engine deceleration rate will be appreciably less than 3,000 rpm per second, although no confirming data are known to the author.

Because of the rigidity of the prime movers, it is essential that the drives incorporate overrunning clutches to permit the alternators to remain in synchronism in spite of momentary deviations in drive ratios or failure of the drives to compensate promptly for rapid changes in engine speed. Partly for the same reason, it is desirable that the drive have some internal slip, either inherent or introduced by the use of a slip coupling such as a fluid flywheel.

Unless the drive has an inherent slip which is consistently reproducible and is independent of the ratio setting, it is necessary that the governor be made load-responsive to provide the speed droop which is required for division of kilowatt load between paralleled alternators. Methods are known for obtaining load division by controlling the relative phase position of the alternator rotors without producing a change in the steady-state speed, but they lack the simplicity and reliability of the speed-droop method. Though the governor may be the droop-determining means, it will have some delay in adjusting itself to a new load level. For this reason some inherent slip in the drive will assist in the maintenance of synchronism during sudden large load transients. Internal slip will also reduce the effect of engine and drive torque pulsations.

While the governor must have a rapid response rate, it must also be well damped to obtain system stability during load and fault transients. Keeping the force-inertia quotient of the ratio-changing device high will make the damping problem easier.

Alternator Design

Experience with the self-excited a-c system has demonstrated a degree of overload capacity and system stability inferior to that which may be expected with separate excitation. The load characteristic of the self-excited machine has a definite knee beyond which the voltage is not self-supporting, with the result that a suddenly applied heavy load may cause loss of voltage, whereas the same load gradually applied may be sustained. Synchronizing difficulty is increased, and the ability to burn minor faults clear or blow limiters is distinctly reduced. Alternators of current design have integral d-c exciters to overcome these handicaps.

Low-resistance damper windings are necessary to ensure freedom from oscillation excited by driving torque pulsation or load transients. Some assistance is given in synchronizing by the full-voltage method because of the damping effect on oscillations caused by closing the circuit breaker at inexact synchronous speed and phase position. When synchronizing is performed by the synchronous motor method, that is, bringing the incoming machine up to approximate speed with no field or with weak field and closing the breaker, low-resistance damper windings are essential, and some benefit is obtained by using continuous end rings. The latter feature will also increase the ability of the alternator to pull back into step automatically under some conditions of out-of-step operation.

It has been suggested that aircraft alternators should be designed with a short-circuit ratio around unity because 60-cycle practice has shown that this gives high inherent stability which will be a benefit in parallel operation and in starting heavy motor loads. The suggestion has merit, but unfortunately it results in a heavy machine. Much of the benefit may be obtained with lighter overall weight by increasing the capacity of the exciter to supply heavy excitation when needed and by making the regulator as fast as possible. Satisfactory experience with self-excited alternators having a short-circuit ratio of 0.60 discounts the necessity of accepting the higher weight of the design which has a higher ratio.

The Voltage Regulator

The use of an exciter brings the regulator duty within the capacity of existing designs of aircraft d-c regulators which have undergone extensive development and combat service during the past two years. What modifications are necessary can readily be made to adapt them to the requirements of the a-c system. If the operating coil is supplied from a three-phase dry-plate rectifier, the regulator will respond to the average of the three average phase voltages. The fact that this is an average rather than a root-mean-square response is not expected to be objectionable with alternators whose harmonic voltage output is within the limits set by the Army Air Forces tentative specifications. The tentative specification for the voltage regulators recognizes the necessity for this average response by stating that the voltage will be checked on type test with a rectifier-type voltmeter calibrated to read root-mean-square values of sine-wave voltages.

The regulator includes a network designed for use in a series-type differential reactive load-equalizer circuit for causing the regulators to equalize the circulating reactive current between alternators operated in parallel. The network is connected to a 125/1-ratio current transformer in the $T3$ lead from the alternator. Zero-power-factor lagging current from the transformer develops across the network a voltage which is in phase and in series with the line voltage applied to the regulator-coil-circuit rectifier. Since this increases the voltage applied to the regulator coil, the regulator acts to decrease the alternator excitation. Conversely, the presence of zero-power-factor leading current will cause the regulator to increase the excitation. Inphase line current produces a network voltage at a right angle to the voltage applied to the regulator and therefore does not affect the operation. Figure 4 illustrates the differential equalizer circuit which has been selected as standard by the interested manufacturers to ensure interchangeability, and this has been incorporated in the tentative specification by the Army Air Forces. With this connection, if the line currents of the respective alternators are equal in magnitude and phase angle, the transformer secondary current circulates solely through the secondary circuits and produces no voltage drop across any of the regulator networks. If one machine delivers more than its share of current, the secondary difference current divides proportionately in the parallel impedance paths formed by the regulator network of the overloaded machine as one branch and the other regulator networks in series as the other branch. With an excess of lagging current delivered by the first alternator, the phase angle of the network voltages in the two branches is such that the excitation of the overloaded machine is reduced and that of the others is increased, with the result that the reactive load balance is restored without affecting the voltage of the system. This lack of effect on the system voltage while correcting a reactive-load unbalance is the advantage which the differential method holds over the somewhat simpler scheme in which balance is achieved by drooping the voltage of each alternator in proportion to the lagging reactive component of current which the machine is delivering, thus producing a droop in system voltage. The differential scheme is identical in principle with the load-equalizing method which is used in present aircraft d-c systems. An improvement over the d-c circuit is the use of a normally closed interlock on the alternator circuit breaker to short-circuit the secondary winding of its associated current transformer when the breaker is open, and so avoid a reaction of the idle alternator circuit upon the regulated voltage of the other loaded machines. To be effective always, this interlock should be designed with the knowledge that it must effectively short-circuit a low voltage. Proper choice of contact materials and contact pressure and the use of several contact points in parallel will avoid trouble caused by contact film and dirt.

Special antihunting or damping means are necessary in the a-c voltage regulator because of the additional field circuit delay which is introduced by the use of an exciter. A very effective method uses a transformer to introduce into the d-c circuit of the regulator coil a voltage proportional to the rate of change of exciter voltage or the rate of change of alternator field current. When introduced with the proper polarity, this voltage causes the regulator coil to anticipate the response of the line voltage, which effect tends to damp out a tendency to hunt or overshoot.[4]

Choosing a Synchronizing Method

The choice of a synchronizing method is somewhat dependent on the prime movers which are used. If the system is driven solely by constant-speed auxiliary engines, a manual or semiautomatic method may be acceptable, because synchronizing is performed infrequently and at a time when the flight engineer is less occupied with other duties. It may be permissible to drop all or part of the load before synchronizing. With main-engine drive, on the other hand, fully automatic synchronizing is definitely desirable, because the availability of electric power is dependent on engine speed conditions, which in turn are determined primarily by warm-up, ground-maneuvering, and flight requirements. When the several alternators come up to speed, the flight engineer may have other duties requiring his attention. With automatic synchronizing, each alternator becomes available for loading as soon as it reaches synchronous speed.

Automatic Synchronizing

Automatic synchronizing equipment has been used in unattended substations and small power stations for many years with excellent service records. For aircraft use the same principles can be applied, but many refinements may be eliminated because of the presence of supervision and the necessity of saving weight. The essential features may be stripped down to means for performing the following functions:

1. Determine when the alternator frequency has approached normal, and initiate the succeeding sequence.
2. Indicate whether the bus is dead or already energized. If it is dead, initiate circuit-breaker closure; or if it is alive,

initiate the synchronizing sequence as follows:

3. Act on the incoming governor to adjust the speed until the difference frequency approaches zero.

4. Close the circuit breaker when the difference frequency is less than about two cycles per second and the alternator and bus voltages are closely in phase.

5. Advance the governor to the normal load setting.

6. Open the circuit breaker at any time when the system frequency drops below the acceptable minimum.

Automatic synchronizing by this full-voltage method may be accomplished with a load on the system, because the human elements of indecision and reaction time are eliminated from the operation. Nevertheless manual controls are provided so that the flight engineer may select the units to be paralleled, or he may trip any alternator circuit breaker at any time. After a machine has been connected to the bus, the flight engineer may, at his leisure, correct any inequality in load division or error in frequency by a manual control which acts through the speed-matching device to adjust the governor setting. Thus he has full control over the operation of the system but is relieved of the necessity of performing the somewhat critical operation of simultaneously matching speed and phase position and closing the breaker at the correct instant. With four alternators he would otherwise have to go through this cycle three times in succession shortly before the time of take-off.

Manual Synchronizing

The weight of the control equipment can be reduced by using manual instead of automatic synchronizing methods, but the saving in weight must be balanced against the increased burden on the flight engineer, the increased chance of an outage caused by faulty manual technique, and the decreased availability of the generating equipment because of failure to connect it to the bus promptly. Perhaps the least objectionable in these respects is a semiautomatic system which differs from the one previously described in that the automatic speed-matching feature is replaced by a manual control. This arrangement requires the attention of the flight engineer at each synchronizing operation but greatly eliminates the factor of faulty manual technique, because the automatic means for determining when the breaker should be closed are retained. The weight saving in comparison with the fully automatic system is approximately two pounds per alternator.

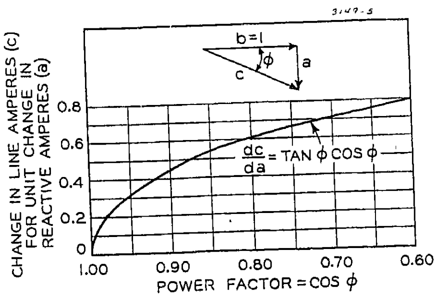


Figure 5. Rate of change of ammeter reading with respect to the reactive component of current plotted against power factor

At normal power factors the ammeter is an insensitive indicator of correct voltage-regulator equalization

Of the straight manual synchronizing schemes which have been proposed for airplane use, perhaps the most foolproof is the one in which the alternator is synchronized in a manner which is common for synchronous motors. The incoming machine is brought up to slightly below synchronous speed with zero or weak field, the circuit breaker is closed, and the machine accelerates toward synchronous speed because of the motoring effect produced by its damper winding. When it is close enough to synchronous speed, it will be pulled into step by its reluctance torque. If this is done with no field excitation, there is a 50–50 chance that it will pull into step with the wrong pole position and will have to slip a pole pitch when field is applied. If the breaker is closed with weak field on the machine, the pulsating current will be higher, but the machine will pull in with the correct pole position. An advantage in starting with weak field lies in the fact that synchronizing lamps may be used as an indication of slip speed while the speed is being raised by manual adjustment of the governor before the breaker is closed.

An important disadvantage of this synchronizing method is the high motoring current which is drawn even at a low slip. Calculations on the present design of airplane alternator indicate that this will equal rated full-load current at about one per cent slip and 160 per cent of full-load current at about five per cent slip. Furthermore, this current pulsates badly at slip frequency. The current calculations were made with the assumption of an infinite bus, and the actual current will be somewhat less. This will increase the time of the system disturbance. The nature of this effect is substantiated by tests which were made during the development of the equipment for the *XB-19* airplane. With one machine on the bus, synchronizing the second machine by this method will be difficult with a load of more than 50 per cent of the alternator rating. It may be found necessary in practice to remove the system load before synchronizing the second alternator.

Adjusting the Voltage Regulators

Experience has demonstrated the difficulty of convincing airplane crews that kilowatt load in an a-c system cannot be balanced between paralleled alternators by adjustment of the voltage regulators. Having had instruction and more or less extensive experience with d-c systems, they must be shown by example that the two systems are not alike in this respect. As serious misadjustment may be encountered, and as this will have an adverse effect on stability because of unequal field excitation, the process of regulator adjustment must be made as simple as possible.

The fact that ammeters are not good instruments to follow when adjusting the regulators is emphasized by Figure 5, which shows how the ammeter reading varies with the changing reactive current, the inphase current remaining constant. At 0.75 power factor the ammeter responds two thirds as fast as the reactive component, whereas at 0.87 power factor the response is only one half as fast as the change in the reactive component. The matter is further complicated at small loads by the fact that a-c ammeter scales are condensed in this region. At normal power factors the inphase component of current has a greater effect on the ammeter reading than the reactive component does, so that a change in power factor or a change in load balance caused by governor aberrations while the regulators are being adjusted will be extremely confusing.

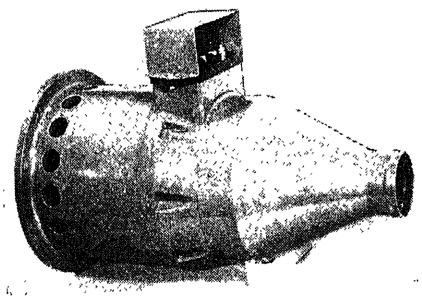


Figure 6. Mock-up model of 40-kva alternator of current design

Because regulator adjustment directly affects the reactive current balance between paralleled machines, varmeters (which read "volt-amperes reactive") give a positive indication of the condition of regulator equalization. Not being responsive to real or kilowatt load, they will not give confusing readings because of governor misadjustment. A varmeter is nothing but a wattmeter with a reactive compensator to shift the potential coil voltage 90 degrees from its true position. Since wattmeters are already necessary in the system to permit supervision of loading and adjustment of the governors, the addition of reactive compensators and a simple switching arrangement make them useful for regulator adjustment also.

Conclusions

A background of experience exists to guide the development of 400-cycle power systems for airplanes, and this knowledge is being used to advantage in the present program of expanding the installed capacity. To duplicate the reliability of the present d-c system, parallel operation of the alternators is required. Parallel operation of auxiliary-engine-driven units already has been accomplished, and, given constant-speed mechanical drives with the correct characteristics, this performance can be duplicated with main-engine-driven alternators. The numerous duties of a flight engineer on a multiengine airplane make it highly desirable to provide automatic synchronizing control to increase the availability of the power system and eliminate outages caused by man failure during the synchronizing operation. Equipment for doing this is available or is being developed.

References

1. Applications of Electric Power in Aircraft, T. B. Holliday. Electrical Engineering, volume 60, May 1941, pages 218–25.
2. United States patent 2,217,473.
3. United States patent 2,262,359.
4. Recent Developments in Generator Voltage Regulation, C. R. Hanna, K. A. Oplinger, C. E. Valentine. AIEE Transactions, volume 58, 1939, pages 838–45.

Principles of Aircraft Electric-Motor Protection

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Introduction

IN writing a paper of this kind for the AIEE, it is needless to say that one part of the aircraft electric-motor protection problem is protection from excessive temperatures. The major problem with the aircraft electric motor is to make available its maximum output before the protector disconnects the aircraft electric motor from the line.

The problem involved in applying an aircraft motor protector for maximum output under all conditions may be realized when it is considered that today an aircraft may be stationed on a field in the tropics and within 24 hours it may be moved to a field well within the Arctic Circle. Or, even more striking, an aircraft may be stationed on a field in the tropics with the sun beating down so as to cause temperatures as high as 70 degrees centigrade in compartments where electric motors are located. In a few minutes this aircraft may be flying at a high altitude where the ambient temperatures may be as low as −60 degrees centigrade.

In domestic, commercial, and industrial uses of electric motors, it has been the aim to protect electric motors so they would not be personal or fire hazards. In accomplishing this, it has been good engineering practice to provide an ample margin of safety. In October, 1942, C. P. Potter presented a paper before the AIEE entitled "The Inherent Overheating Protection of Single-Phase Motors" from which I quote his comments regarding the summary of the data for the tests under various loads, voltages, speeds, ambients, and amounts of ventilation:

> "Conclusions
>
> The variations in temperature shown in the table are small and lead to the conclusion that inherent overheating protective devices mounted inside fractional horsepower single-phase motors completely protect them against all abnormal operating conditions."

There are many engineers who believe that all motor protectors will limit the work output of electric motors, and they advocate that the operator should have the privilege of destroying the aircraft electric motor if by so doing he thinks he can accomplish his mission. It is questionable if any operator has the ability to judge properly during the stress of emergency as to when he should or should not destroy a motor.

A good automatic device will always repeat its performance, and in this paper it will be assumed that a properly designed and properly applied electric-motor protector must not place any limitations on the potential work output of the electric motor.

Stated in the simplest terms, the problem is to establish principles to follow in protecting aircraft electric motors from destructive temperatures without limiting output regardless of the loads or the ambient encountered.

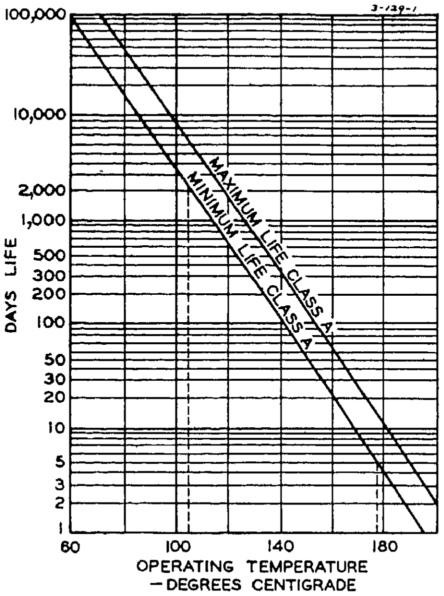

Figure 1. Effect of temperature on life of insulation

While there are many classes of insulation in use in aircraft electric motors, many of these electric motors have some class *A* insulation in them. There is more information available on class *A* insulation, and for the purpose of this paper the consideration of any other class of insulation would be a matter of degree and would not change the principles to follow in having protection without limiting output.

Since the first use of electric motors, there has always been this problem of temperature protection. First, fuses were used, then current-sensitive devices mounted remote from the electric motor, and since 1929 there has been an increasing use of built-in electric motor protectors. E. J. Schaefer, formerly of the

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fractional motor engineering department of the General Electric Company, in a paper entitled "Protection of Automatically Started Fractional Horsepower Motors," published in the September 1940 issue of the *News-Bulletin*, of the International Association of Electrical Inspectors, shows that only with a properly built-in protector can there be protection and at the same time approach maximum motor output under any possible load condition.

Many aircraft electric motors have intermittent-duty cycles and in order to conserve weight the motors are operated at high speeds, and these intermittent-duty motors are regularly operated at high overloads as compared to a continuous running rating. This results in motors of small size which presents a space limitation for the location of the protector.

Location of Protector

It takes no data or lengthy discussion to convince one that theoretically the proper location for the protector would be at the danger hot spot where it would be responsive to the danger hot-spot temperature. Because of physical limitations, this is impossible; so, there must be a compromise by preparing a location when designing the motor body that will give the thermostatic element the best possible opportunity of absorbing heat from the danger hot spot.

Temperature at Which to Protect

Regardless of the kind of insulation used, insulation life is dependent on both time and temperature. This is best illustrated by the life-temperature curve for class *A* insulation, Figure 1 of this paper, copied from page 434 of the book "Insulation of Electrical Apparatus," by Douglas Miner.

Class *A* insulation is listed by AIEE as good for a maximum safe operating temperature of 105 degrees centigrade. From the curve, it can be seen that this will give a minimum expected life of 2,000 days. The tentative Army-Navy (AN-M-10) electric motor specifications require that the protectors provide a minimum life of five continuous hours of operation under locked rotor conditions without damage to the motor. To provide sufficient manufacturing tolerance, it would be desirable to select a temperature corresponding to a minimum life of five days. This gives a temperature of 178 degrees centigrade.

Even with the selection of this high maximum allowable operating temperature for five days' life of the insulation, it should be remembered that in normal applications the actual operating temperature of the motor will be far below this value. This is best illustrated by Figure 2.

Line *A* represents room ambient of 25 degrees centigrade.

Line *B* represents the maximum winding operating temperature of 105 degrees centigrade under normal loads and 25 degrees centigrade ambient temperatures.

Line *D* represents the maximum winding temperature at which there is a tendency for most engineers to protect the motor. If this temperature of 120 degrees centigrade were selected, it is obvious that a small overload or an abnormally high ambient would cause nuisance tripping of the protector as there is very little winding temperature difference between full load and the point where the protector would open.

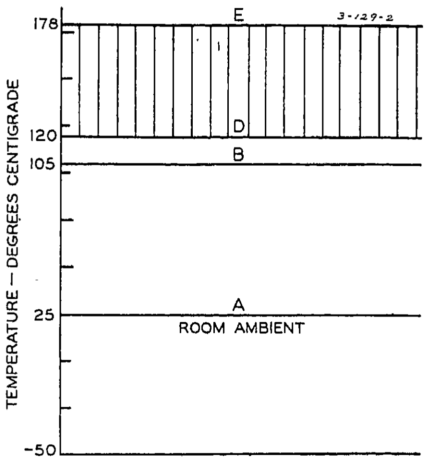


Figure 2. Chart of temperature levels involved in aircraft electric-motor protection

Line *E* is the 178 degree centigrade temperature selected for a minimum of five days' life from Figure 1.

To get protection without limiting the work output of the motor, the protector must control the motor winding temperature at 178 degrees centigrade, line *E*. The shaded area between the curves *D* and *E* represents the thermal capacity which most motor designers would not use. Thus, if protection were applied on the basis of line *D*, representing maximum winding temperature, it would lead to the condition where the aircraft designers (and others) would know that there was additional capacity available in the motors as indicated by the shaded area, and he (or they) would then rather have the operator run the risk of burning out the motor than be limited in power output as indicated by line *D*. The values used in this paper are on the basis that the winding temperature is the limiting hot spot; other hot spots would have different hot-spot maximum temperatures.

The Danger Hot-Spot Temperature

The temperature that will first cause damage to an aircraft electric motor need not be the winding temperature as indicated heretofore, or even the highest temperature in the motor. The first damage may be at the brushes, the solder, or the shunt leads to the brush-holder, and so forth. It is necessary to determine where the danger hot spot is and how hot it gets. This may be estimated at the time of designing, but the actual data can be obtained only by test with thermocouples and other temperature-measuring devices located at every point of possible damage.

Figure 3 is a photograph of such a test setup with eight thermocouples and four voltage-drop leads mounted in the motor. To be complete, these data should be obtained not only at room ambient but also at the maximum and minimum ambients at which the motor is expected to operate, and with various loads from the maximum continuous load the motor can carry without exceeding the maximum allowable hot-spot temperature through and including locked rotor. This is necessary because, for example, under locked rotor the armature windings may be the danger spot, and during running the danger spot may be shifted to the solder on the commutator.

Thermal-Element Operating Temperature

We know that the heat dissipation, or in other words the capacity of a motor, will vary with ambient. The accurate calculation of the capacity at different ambients would involve the use of a number of empirical values based on test data which are not available today. As the major portion of the heat is generated in the copper windings, a simple method of indicating the trend would be to use the following formula:

$$R_x = R_n\sqrt{\frac{T_m - T_x}{T_m - T_n}}$$

R_x is the current rating at a given ambient.
R_n is the current rating at room temperature (25 degrees centigrade).
T_m is the maximum allowable temperature (178 degrees centigrade).
T_n is the room temperature (25 degrees centigrade).
T_x is the given ambient temperature.

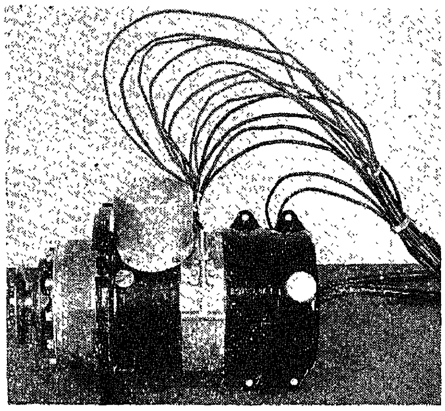


Figure 3. Aircraft electric motor equipped with thermocouples and resistance leads

The maximum allowable winding temperature previously adopted in this paper was 178 degrees centigrade. It is assumed that an intermittent-duty motor when carrying 80 per cent of its rating continuously at normal room ambient (25 degrees centigrade) will have a temperature rise of 153 degrees centigrade, resulting in an operating temperature of 178 degrees centigrade.

The curve in Figure 4 is obtained by using these values in the formula. It has been assumed that this intermittent-duty motor when running continuously would only carry 80 per cent of its rating at 25 degrees centigrade ambient. It would carry more load at lower ambients as indicated by the curve at −50 degrees centigrade, where it will carry 97 per cent of its rating continuously. At high ambients the motor output would be limited below the 80 per cent. At 70 degrees centigrade ambient the curve shows the motor will operate continuously at 67.5 per cent of rating. At an ambient of 178 degrees centigrade, the motor should not operate at all because the hot spot is already up to the maximum allowable temperature.

The information in this curve may be stated as follows: At any ambient there is one value of continuous load current that will bring the danger hot-spot temperature up to the maximum allowable value of 178 degrees centigrade (line *E*). If the protector is going to allow the motor to attain this temperature (178 degrees centigrade) at all ambients before removing the motor from the line, a protector will be needed that will have the same ambient characteristics as the motor it is to protect. As the operating temperature of the thermal element must be a fixed value, and if it is to match curve Figure 4, the thermal element must operate at 178 degrees centigrade when carrying zero load. This then establishes the operating temperature of the thermal element at 178 degrees centigrade. It can be stated that theoretically for continuous running loads the operating temperature of the thermal element of the protector should be the same temperature as the temperature of the hot spot to be protected.

Function of the Heat Generated in the Protector

When discussing the location of the protector, it was stated that it was physically impossible to locate the protector at the hot spot. With the protector located in a compromise position, it cannot be expected that the protector will be heated by the temperature of the danger hot spot up to the hot-spot temperature. This is illustrated graphically in Figure 5.

Curve *M*, the ambient temperature versus motor continuous carrying capacity, is the calculated curve from Figure 4, and with a protector mounted in this motor, the curve of the protector temperature, that is, the temperature of the metal parts of the motor which surround the protector, would fall between curve *M* and curve *E*. The exact location of curve *P* would depend on how sensitive the protector was to the hot-spot temperature it is to protect. As the protector is a part of the motor assembly, the shape of the protector temperature curve *P* will be similar to curve *M*. But, regardless of where the protector is mounted, the protector location temperature under continuous operation would be greater than the ambient temperature curve *M* and less than the operating temperature of the thermal element curve *E*.

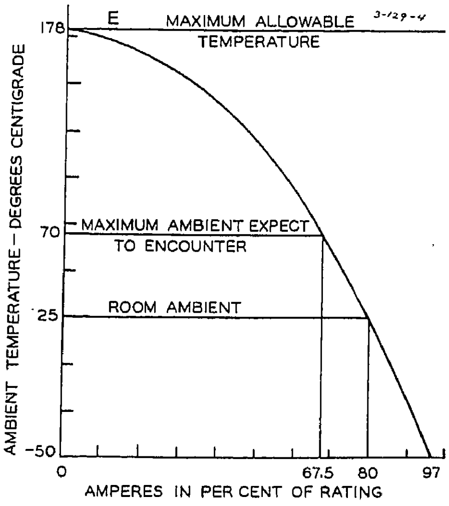


Figure 4. Ambient versus per cent load curve

In order to use a thermal element operating temperature of 178 degrees centigrade, some method must be used to raise the thermal element temperature from that temperature surrounding the protector, curve *P*, up to 178 degrees centigrade. This can be accomplished by generating heat in the protector. Figure 6 is a schematic diagram of a protector with a heater built in for this purpose. This heater is connected so that the load current passes through it and the thermal element, and the heat generated in the protector is proportional to the current flowing at various loads, thus furnishing a variable temperature rise to the thermal element depending on the load current. Therefore, at any ambient there is a per cent of motor load current that will not only cause the hot-spot temperature to reach 178 degrees centigrade, but that same value of current can be used to cause the thermal element to reach the same temperature.

To illustrate, take the point at 70 degrees centigrade ambient. From curve 4, 67.5 per cent rated current will raise the danger hot-spot temperature to 178 degrees centigrade and from curve Figure 5 at this same 70 degrees centigrade ambient, the motor heats the protector to 125 degrees centigrade and the heat generated in the protector must raise the thermal element up to 178 degrees centigrade. Therefore, it can be seen that the horizontal crosshatched area indicates the temperature rise imparted to the protector by the heat of the motor and the vertical crosshatch indicates the temperature rise that must be imparted to the thermal element by the heater.

Ultimate Trip

So far, continuous performance of the motor has been discussed, and, if, after running sufficiently long to reach maximum stable conditions for any given

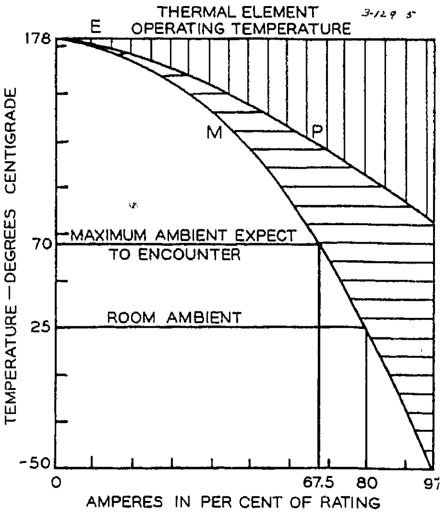


Figure 5. Motor ambient and protector temperature curves

ambient, there would be some change in these stable conditions, such as a rise in the ambient temperature or a slight increase in load current, both the hot-spot and the thermal-element temperature would rise, but as the protector would operate at any temperature in excess of 178 degrees centigrade, the motor would have reached an *ultimate* running condition as the protector would remove the motor from the line. This is called *ultimate trip* and may be stated as that value of load current which at a given ambient will cause tripping of the protector when the motor has reached a stable condition.

Locked Rotor

Locked rotor is the most severe load condition that can be imposed on the motor. Therefore, it is necessary that the protector take care of this condition in a satisfactory manner. For continuous running, the temperature of the protector would not follow the hot-spot temperature. So, under locked rotor, the protector temperature will lag still further behind the hot-spot temperature. This is illustrated in Figure 7.

F is the time-temperature curve of the danger hot spot for locked rotor. Since the locked-rotor current is high, the temperature will rise so rapidly that the protector (curve *G*) absorbs very little temperature from the motor before the hot spot has reached a dangerous temperature. This means that when the rotor is locked, the heat generated in the protector must furnish all of the temperature rise to the thermal element to bring it up to 178 degrees centigrade. The shaded area of Figure 7 shows this temperature rise that must be furnished by heat generated in the protector for 25 degrees centigrade ambient.

Some engineers think that locked-rotor protection is all that is needed for d-c aircraft electric motors. This thinking is based on the fact that the duty cycle of most aircraft electric motors is very short, and the major troubles come from jammed mechanisms which cause stalling of the rotor. Then, as soon as the rotor stops, it heats rapidly as shown in curve *F* of Figure 7. With a constant voltage supply, the current falls off because of the increased resistance of the windings, and the motor starts losing torque.

If protecting the motor from destructive temperatures was all there was to consider, a protector that provided locked-rotor protection would be a satisfactory protector, and holding the motor on the line for only one second would be sufficient time to let the motor start if free to do so. In aircraft there are many times when mechanisms are not jammed but are damaged sufficiently to introduce loads approaching blocked conditions, and at the same time the motor is able to turn over slowly. While it may take a relatively long time for it to complete its work, the chances are that the motor will do some good if it can be kept operating without burning out. This means that for locked-rotor conditions, the motor should not be taken off the line until the danger hot spot has reached its maximum allowable temperature. In providing locked-rotor protection without regard to the maximum output, it is possible either to overprotect, so as to cause nuisance trip-outs, or to underprotect so that the motor will burn out at normal loads or running overloads.

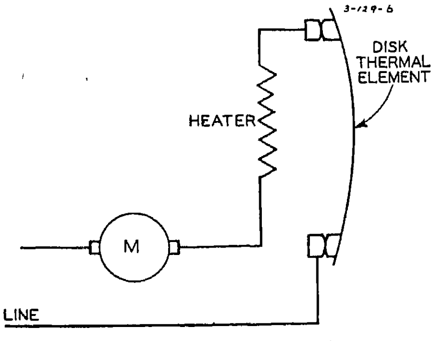


Figure 6. Schematic diagram of heater-type motor protector

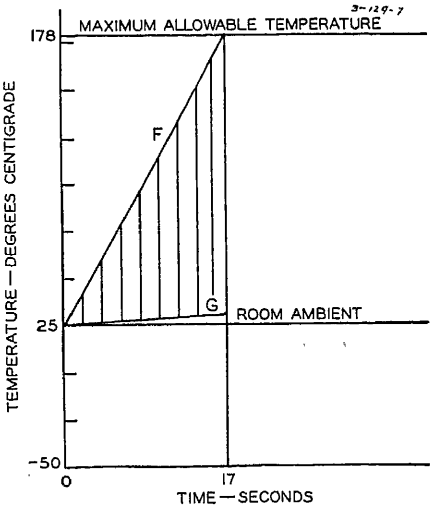


Figure 7. Heating curve for locked rotor

Overprotection is illustrated in Figure 8. Here the motor is carrying an excessive load but has not reached a stalled-rotor condition before the danger hot spot reached the maximum allowable temperature. The curve *J* is the time-temperature curve under a high running overload, and *K* is the time-temperature curve of the protector location. If this protector were one that took the motor with locked rotor off the line in one second, then under this extreme running condition the protector would have taken the motor off the line in probably four seconds, and the motor output would be limited to a small amount of work, and the danger hot spot would have been only 35 degrees centigrade. This kind of locked-rotor protection would not provide the maximum possible output of the motor. By proper design of the protector as to the heat generated in the thermal element, the operating temperature of the heater, the amount of heat, and its location with respect to the thermal element, it is possible to have some control over the rate of temperature rise of the thermal element so that regardless of the load conditions the protector will let the danger hot spot approach its maximum allowable temperature before disconnecting the motor from the line. Under these conditions, the motor can give maximum work output with protection from destructive temperatures regardless of load current or ambient.

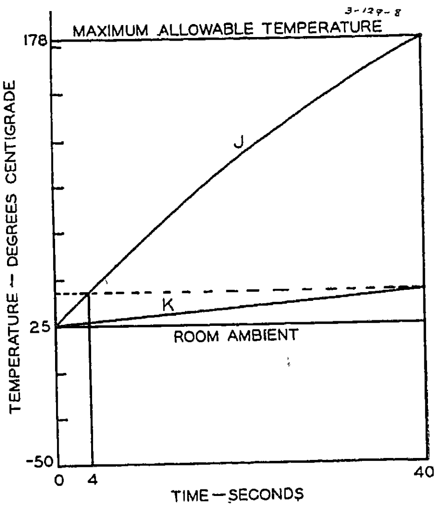


Figure 8. Heating curve at high overloads

Loads Less Than Maximum Continuous Running

Any load below the continuous value that will not heat the danger hot-spot temperature up to its maximum allowable temperature has not been considered in this paper, for, obviously, no protection would be needed for this condition.

Intermittent Loads

Up to this point, the two extremes of load that would call for protector operation to protect the winding hot spot have been discussed:

1. The maximum continuous running load.
2. Locked rotor.

Any value of intermittent load that would call for protector operation in protecting the winding hot spot would fall between these two extremes and need not be given any special consideration.

Regardless of the ambient conditions or load above maximum continuous running, it would be expected that a motor with an inherent overheat protector designed and applied according to the principles laid down in this paper would give a constant maximum danger hot-spot temperature as illustrated in Figure 9.

Curve *N* is the temperature of the danger hot spot for any value of overload or locked rotor and shows the same hot-spot temperature for all ambients.

In following out these principles, protector and motor designers are confronted with limitations, such as

1. Applying protectors to existing motors.
2. Limitations as to materials for the insulation of the protector.
3. Space limitations.
4. Physical properties of materials for the thermal elements and for the heaters.
5. Manufacturing tolerance more particularly of the motor than the protector.

In view of these limitations, some deviation would be expected from the theoretical curve *N* in Figure 9.

The curves in Figure 10 (results of tests on a protected aircraft electric motor) show how surprisingly close a protected motor can come to the theoretical conditions when a protector is designed and applied in line with the principles of this paper.

Conclusions

In conclusion, these principles can be summarized as follows:

1. Prepare a protector location when designing the motor.
2. Select those points in the motor which are likely to be the danger spots and assign to each such point the maximum temperature possible.
3. Locate the danger hot spot by test with thermocouples and resistance rise measurements.
4. Adopt a thermal-element temperature as close as possible to the protection temperature of the hot spot to be protected.
5. Do not overprotect under any load condition; let tolerance be on the side of a possible loss of a motor now and then rather than rob all motors of a given design of any potential capacity.
6. Supply the difference in temperature between protector location and the thermal-element operating temperature by a heater built into the protector.

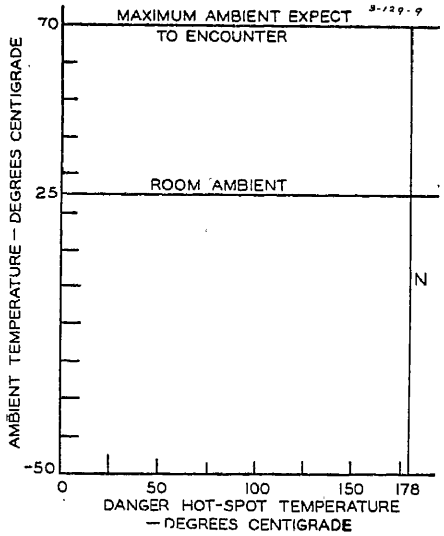


Figure 9. (Theoretical inherent protected motor) ambient versus hot-spot temperature for maximum safe load or overload

7. When using a protected motor, do not undermotor an application.

Men's lives depend on the continued functioning of aircraft electric motors. Restricting the output of an electric motor is poor engineering; a burned-out motor is a total loss; protect them so that all allowable output of aircraft electric motors is available to the crews.

Supplement

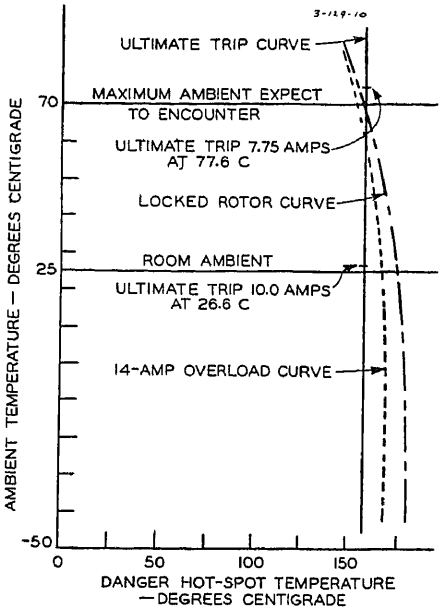
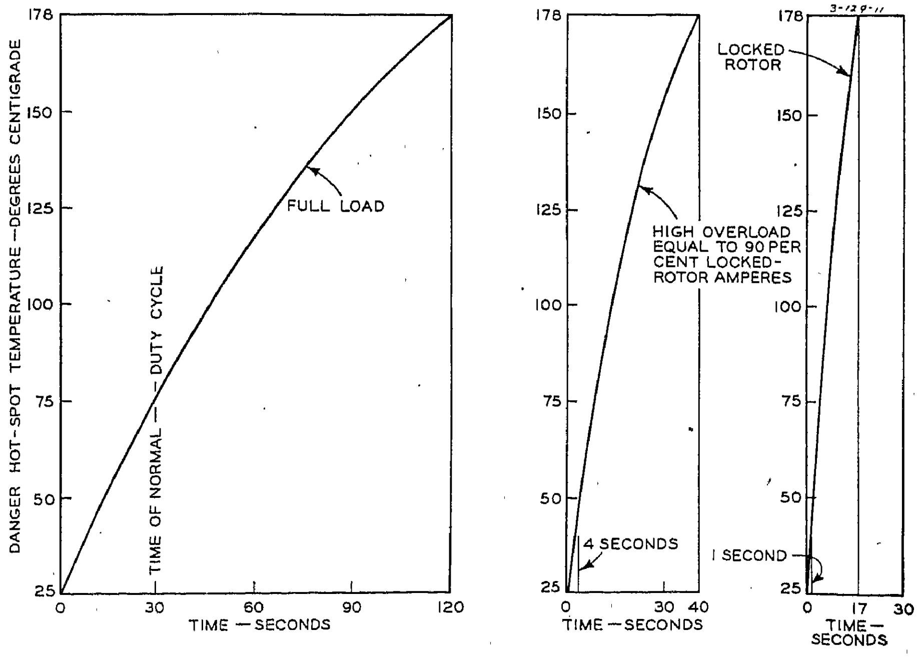


Figure 10. Actual results—inherent protected motor—ambient versus hot-spot temperature for any maximum safe load or overload

Since writing this paper, I have had a number of discussions with various people, and as a result of these discussions I have made some theoretical curve sheets which are in this supplement. These curves have particular reference to the results of overprotection and to the results that are likely to occur when one makes an application for locked-rotor protection only.

On curve sheet Figure 11, at the left, is the theoretical time-temperature curve for an intermittent-duty 24-volt d-c motor, run at an ambient of 25 degrees centigrade and with full load current. It is to be noted that the duty cycle is 30 seconds and that it takes 120 seconds for the hot spot to get up to the assumed dangerous temperature of 178 degrees centigrade. From the data on this curve, it is evident that as long as everything

Figure 11. Theoretical time–temperature curves for intermittent-duty 24-volt d-c motor at 25 degrees centigrade ambient temperature



runs normally no protection is required. The motor cannot be overloaded, and it cannot get too hot.

At the extreme right is the theoretical time-temperature curve for locked rotor. Here, we see that the motor will be on the line 17 seconds before the hot spot reaches the assumed dangerous temperature of 178 degrees centigrade.

When a protector is applied to give locked rotor protection only, there is a tendency to set the protector down to as short a time as possible.

In these d-c motors it only takes a fraction of a second for them to come up to speed so that a protector which cuts off at one second would allow ample time for the motor to start and come up to speed, providing there was no excessive load on the motor. A protector with this type of characteristic would give protection for locked rotor, and, if the rotor was definitely locked because of jamming in the mechanism, there is nothing to be gained by leaving the motor on longer than the one second.

In the center curve of Figure 11 we have a theoretical time-temperature curve of this motor when the load is such that the current is approximately 90 per cent of the locked-rotor amperes. Here the motor is running and doing some work, although the speed may be very slow, yet, if kept on the line long enough, the duty cycle could be completed. This would be particularly true if the motor under this running condition were allowed by the protector to run up to its maximum assumed safe temperature of 178 degrees centigrade. In this case, you will note that it would take 40 seconds to attain this temperature. The protector would cut "on" and "off" and the motor would run for a while, and then stand stationary for a while, and then run, and so forth, until the unit had an opportunity to complete the cycle.

On the other hand, if this motor were protected with a protector that cut off at one second under locked conditions, then four seconds would be the maximum that we could expect the protector to let the motor run with 90 per cent of locked amperes, and here we would have such a short "on" cycle that the length of time required for the unit to complete its duty cycle would be so great that it is extremely doubtful if any useful work could be obtained from the motor at this or any other high overloads.

On curve sheet Figure 12, these two types of protector performances are illustrated by the "on" and "off" cycles. In the curve at the top of the page, the protector allowed maximum output of the motor and in the one at the bottom of the page, the protector was applied for locked-rotor protection only (one-second cutoff). By accumulating all of the "on" times, we see that at *A* we have an accumulated running time shown as the result for six cycles of protector operation, whereas at *B* we have the accumulated time for 15 cycles, the 15 cycles having taken place in the same length of time as the six cycles shown.

From experimental work done so far, it appears as if there is considerable likelihood of obtaining the poor performance indicated by applying thermal protectors for locked-rotor protection only. Therefore, it is believed that one of the most important considerations in connection with aircraft electric-motor protection is to make sure that the protector allows the motor to give its maximum capacity under all conditions of load and ambients.

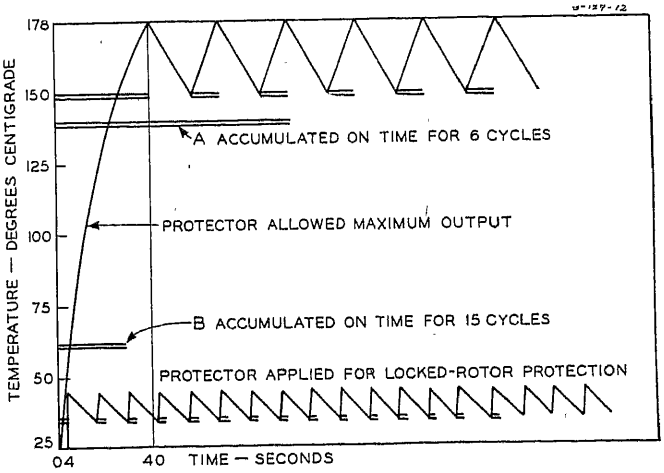


Figure 12. Theoretical time–temperature curves for intermittent-duty 24-volt d-c motor run at 90 per cent locked-rotor amperes

The Testing of Mercury-Arc Rectifiers

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THE design of mercury-arc rectifiers is based, as is that of all electrical apparatus, upon knowledge gained from fundamental principles, test data, and operating experience. Our knowledge of the physical action occurring within the rectifier is very limited. While the fundamental processes which take place in an arc discharge have been quite completely described by physicists,[1] their complexity has prevented their expression in a form usable by the engineer. For this reason rectifier design depends, to a larger degree than does the design of most electrical apparatus, upon experimental data obtained from tests.

Of the actions occurring within the rectifier, the phenomenon of arc-back is particularly important as it plays a dominant role in the testing, rating, and performance of mercury-arc rectifiers. Various theories have been proposed in an endeavor to account for arc-backs, and the more recent of these appear to agree quite well with observations and experience.[2,3] Despite these advances, the causes of arc-back are not yet sufficiently well understood so that the effect of specific modifications in the construction may be reliably predicted.

Special testing procedures are required to meet the unique conditions encountered in rectifier operation. Arc-back behavior is a primary consideration in the formulation of load tests involving the determination of arc-back rate. Some correlation has been observed between duty on the rectifier, condition and construction of the rectifier, and frequency of arc-back, but the occurrence of arc-back is essentially random in nature, not only under definite limiting conditions, but under all conditions of rectifier loading. The determination of arc-back rate from load tests is, therefore, a statistical problem. The magnitude of the problem may be appreciated from the fact that one arc-back per month on a six-anode rec-

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tifier operating at 60 cycles is but one failure per billion cycles. It is obviously impractical to check such performance by operation under normal conditions, so special testing methods must be devised.[4]

The primary purpose of this paper is to describe the procedure used in testing mercury-arc rectifiers during both development and manufacture. Tentative standards for testing rectifiers are covered by report on AIEE Standard 6 for "Acceptance Tests for Metal-Tank Mercury-Arc Rectifiers," issued in 1934. However, these standards do not cover recent developments in testing technique.

The nature of the electrical and mechanical limitations of the rectifier is discussed in an endeavor to provide a basis for the evaluation of results of tests in terms of expected operation in normal service. The basis for the rating of rectifiers is also discussed and data are presented showing the results of tests on standard rectifiers.

Purpose and Scope of Tests

Tests on mercury-arc rectifiers may be classified under three general heads, depending upon the purpose for which they are made; namely, developmental, commercial, and field tests.

Developmental Tests

Since the fundamental knowledge is not sufficiently complete to provide a satisfactory design basis, new rectifier designs are based upon experience gained from previous designs supplemented whenever necessary by tests bearing directly on the problem at hand. Developmental tests may range from general studies of the physical action to specific tests on a full-size sample of the new design.

Examples of such general studies are: heat transfer and the distribution of losses; vapor pressure, and vapor flow; ionization during the inverse cycle; inverse current to the anode; phase occurrence of arc-back; current distribution during the conduction period; magnetic effects; and so forth. Examples of specific tests are: mechanical, electrical, and thermal measurements on new materials and new structural arrangements for component parts, load tests, control tests, and so forth.

The development of a new rectifier design usually requires the construction of a full-size sample. Developmental tests are made on this sample in order to determine the modifications required to obtain the desired performance. The complexity of the physical action and the lack of complete knowledge of the fundamental processes preclude the use of scale models in making rectifier tests.

Commercial Tests

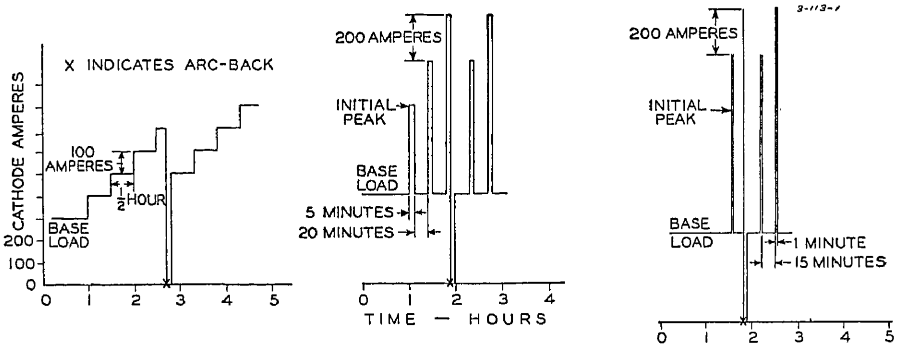
These are tests made on commercial rectifiers during manufacture for the purpose of

1. Checking the quality of manufacture.
2. Determining the characteristics of the rectifier equipment.
3. Checking the over-all performance.

Field Tests

Despite the steady accumulation of rectifier knowledge and experience, it is still not possible to predict or guarantee in advance the exact performance of a rectifier in the field, particularly as regards the frequency of arc-back. Rectifiers of proved ample capacity and good design may operate with excessive arc-backs when first put into service in new installations, and it is sometimes found necessary to make minor adjustments in the field to obtain proper performance.

Field tests are usually directed at factors not covered by the developmental tests, for example, the effects of longer periods of continuous loading than can be undertaken in the factory tests or different service conditions. Furthermore, field tests must be arranged so that they may be carried on concurrently during normal operation.



A. Continuous load limit
1. Start at base load specified
2. Increment, 100 amperes every half-hour, approximately five per cent of basic load
3. Increase load until arc-back occurs
4. Reduce load 200 amperes and continue as before
5. Continue until three successive arc-backs are obtained at substantially equal loads, indicating that limit has been reached

B. Five-minute load limit
1. Start at base load two thirds of continuous load limit
2. Initial peak, 200 amperes greater than continuous load limit
3. Duty cycle—20-minute base load, five-minute peak, repeat
4. Increment, 200 amperes on successive peaks
5. If arc-back occurs, reduce load 200 amperes and continue as before until three arc-backs are obtained at limit

C. One-minute load limit
1. Start at base load two thirds of continuous load limit
2. Initial peak—200 amperes greater than five-minute load limit
3. Duty cycle, 15-minute base load, one-minute peak, repeat
4. Increment, 200 amperes on successive peaks
5. If arc-back occurs, reduce load 200 amperes and continue as before until three arc-backs are obtained at limit

Figure 1. Procedure for load-limit tests

List of Tests

A list of the more important tests used in the development and manufacture of mercury-arc rectifiers follows:

1. Shop tests.
Vacuum tightness.
Degassing.
Dielectric tests.

2. Load tests.
Load-limit tests.
Load runs.
Duty cycle.

3. Short-circuit tests.
Current limit.
D-c short circuit.
Simulated arc-back.

4. Control tests.
Pickup action.
Blocking action.
Phase control.

5. Performance characteristics.
Efficiency.
Voltage regulation.
Power factor.
Wave form.

Shop Tests

In the manufacture of pumped rectifiers the first tests made upon the completion of assembly are vacuum tightness and degassing. These tests provide a check on the basic structural quality and condition the rectifier for full voltage operation. Together with the dielectric tests they comprise the ordinary short commercial tests.

VACUUM-TIGHTNESS

The vacuum tightness is checked by taking seepage tests. These tests provide a measure of the tightness of the tank materials, welds, insulating seals, and gasket joints and are usually made both before and after degassing. The test procedure consists in evacuating the rectifier, then shutting off the vacuum pumps with the rectifier at room temperature and noting the increase in pressure over a definite period, usually six hours.

DEGASSING

While the rectifier degassing may be considered an essential part of the manufacturing process, inasmuch as the rectifier must be conditioned before it will operate at full voltage, the degassing operation also serves as a test of various features of the rectifier. For example, it provides a further check on the soundness of the welds, as the steel parts of the rectifier vacuum tank are heated to operating levels for the first time. A weak weld which appeared tight on the first seepage test may show a leak on the second seepage test. The time required for degassing is a measure of the pumping speed of the vacuum pumps. Some indication of the current capacity of the component parts of the rectifier assembly may be obtained by observing the temperatures of the various parts during degassing. The operation of the cooling system may also be checked during degassing since the rectifier losses at any specified current are substantially the same as during full voltage operation.

DIELECTRIC TESTS

High-voltage tests are made in the same manner as for most electrical apparatus using a 60-cycle alternating voltage. Such tests provide a check on the correctness of assembly and the soundness of the solid insulation.

Present AIEE Standards[5] call for the application of high-voltage tests after the rectifier has been thoroughly evacuated and degassed. It is not always possible to make tests under this condition because of the occurrence of breakdowns resulting from glow discharge. The voltage required for breakdown between electrodes in a vacuum may differ considerably from that required for breakdown at atmospheric pressure. Where difficulty of this kind is experienced, it has been the usual practice to make dielectric tests with the rectifier filled with air at atmospheric pressure.

Numerous attempts have been made to correlate the high-voltage break-down characteristics of a rectifier with the occurrence of arc-backs. Tests have been made with an alternating voltage and also with a negative direct voltage on the anode, increasing the voltage until breakdown occurred. Measurements of insulation resistance have also been made. While it is possible to determine the insulation strength and other characteristics of the solid insulation by such tests, they give no indication of the rectifier performance as regards the occurrence of arc-backs.

Load Tests

The purpose of load test on rectifiers is to determine their quality in terms of capacity and reliability. The simple structural abilities are readily evaluated by loading the rectifier and making thermal and mechanical measurements. But the arc-back rate cannot be determined by operating at normal loading because of time limitations. Theory indicates and experience has shown that the performance of a rectifier under normal conditions may be gauged from tests made under more severe conditions of loading.

There are two general types of load tests. One is the accelerated load test, or "load-limit test" as it has been termed, which is used primarily to determine the capacity of the rectifier. The other is the "load-run" or duty-cycle test, which is used for checking the reliability and quality of manufacture. The inherent difference between these tests lies in the method of loading. The procedure in the case of the accelerated test is to increase the loading gradually until failure occurs, either structural or arc-back, while that for the load-run test is to maintain a predetermined loading for a definite period and observe the operation.

The loading or duty on a rectifier involves a number of factors, among which are the following:

1. D-c voltage.
2. Load current.
3. Amount of phase control.
4. Type of circuit and circuit reactance.
5. Duration of loading.

All of these factors must be considered in the application of load tests. However, the usual procedure is to make tests at a given d-c voltage, such as 600 volts, and vary the loading by changing the load current. The amount of phase control may also be varied. Tests are usually limited to a single d-c voltage and one transformer circuit.

A further factor which is usually varied during load tests is the rectifier control temperature as the rectifier performance is a function of its temperature.

LOAD-LIMIT TESTS

Load-limit tests are made by applying a gradually increasing load on the rectifier until failure occurs, either structurally or because of arc-back. A typical loading procedure which has been used successfully in the development of a number of new rectifiers is shown in Figure 1.

Several factors must be considered in the choice of loading procedure. One of the more important of these is the duration of loading. The thermal storage capacity of a rectifier is small relative to its losses and the thermal time constant is about $^1/_2$ to 1 hour. A one-half-hour interval was, therefore, selected for the continuous test. Shorter intervals were chosen for the five-minute and one-minute tests on the basis that substantially normal temperatures may be obtained by operating at reduced load between peaks. A set of continuous five-minute and one-minute load-limit tests using these time intervals may usually be made in an elapsed time of two or three days.

Another factor is the effect of aging upon rectifier performance. It is well known that the arc-back rate on a rectifier, during initial operation at full voltage immediately following degassing, may be greater than that obtained subsequently after several weeks' or months' operation. This improvement in operation is due to further conditioning occurring under load. This conditioning effect is hastened by operation at heavy loads such as are applied during load-limit tests, and it is, therefore, desirable to obtain several arc-backs at approximately the same load values in order to insure that the load limit has been reached.

It is not always possible to increase the rectifier loading sufficiently to obtain arc-backs. In some types of construction, parts of the rectifier, particularly those in the arc path, may be permanently damaged by excess loading, and this damage may occur before arc-back is obtained. In multianode rectifiers the cathode construction is frequently limiting. However, it has been found desirable, wherever practical, to design the

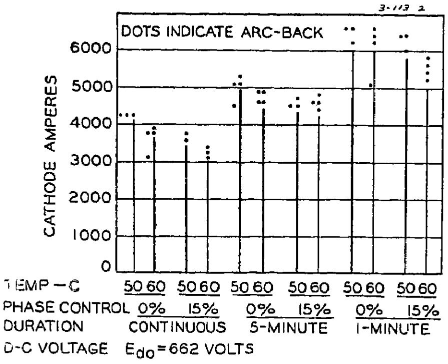


Figure 2. Results of load-limit tests on three ignitron tanks

rectifier so that damage of this kind will not occur, and the load limit is fixed by arc-back. A sturdy construction, capable of operation until arc-back occurs, facilitates testing considerably and permits full attention to the problem of arc-back.

The results of load-limit tests on three ignitron tanks are shown in Figure 2. The standard rating for a rectifier unit consisting of six of these tanks is 2,500 kw at 625 volts. The procedure described in Figure 1 was used in making these tests.

Single anode rectifiers like the ignitron or excitron, may be tested in sets of three since each tank is separate and operates independently. The circuit action, insofar as the voltage and current waves impressed on the anodes are concerned, is substantially the same when three tanks are operating on a single wye as when they are operating as a part of a multiple rectifier, for example, in a double-wye circuit. Three-anode operation facilitates rectifier testing considerably and provides a substantial reduction in time, labor, and power requirements. A large amount of the developmental work on the ignitron has been carried out on three tank units.

The results of load-limit tests on a six-anode multianode rectifier are shown on Figure 3. This rectifier has a standard rating of 1,500 kw at 3,000 volts. Although differing in detail, the procedure used for this test was similar to that already described.

In the case of multianode rectifiers, it is necessary to load the complete rectifier, inasmuch as the cathode and tank are common to all the anodes, and the general physical action in the tank is a function of the cathode current. Also, the ionization attributable to current in one anode affects the neighboring anodes. However, a reduction in the power capacity required for testing a multianode rectifier may be effected by passing current through some of the anodes at low voltage on degassing connection.

The effect of temperature upon rectifier capacity is clearly shown by the load-limit tests for the multianode rectifier, Figure 3. These curves indicate that there is an optimum temperature at which the rectifier capacity is a maximum and that the capacity decreases at higher or lower temperatures. Research[6] has shown that in the lower part of the temperature range the rectifier capacity is defined by a "current limitation" and the maximum current which the rectifier can carry is that which utilizes all the molecules in the mercury vapor as positive ions to neutralize the space charge. When this current is exceeded, the arc drop increases abruptly and arc-back ensues. In the upper part of the temperature range arc-backs occur as a result of voltage breakdown because of high mercury-vapor pressure. The voltage which the rectifier will withstand without arc-back decreases as the vapor pressure increases with rise in temperature. At the higher temperature the rectifier capacity is defined by a "voltage limitation." It is generally desirable to operate a rectifier at as high a temperature as possible without incurring arc-backs.

Load Runs

Load runs are made by applying a predetermined loading for a definite period and observing the operation, for example, counting the number of arc-backs which occur. Such runs are usually made with a constant loading applied for a period of 12 or 24 hours. Load runs at rated load or at loadings in excess of rating provide a check on rectifier performance under conditions approximating those encountered in service. While the period of loading is too short to determine the arc-back frequency on a successful rectifier and the service conditions may not be fully duplicated, such tests do indicate the maximum arc-back rate likely to be incurred in service.

In cases where load-limit tests cannot be applied because of structural or thermal limitations, lack of testing facilities, and so forth, the rectifier capacity may be determined by means of load runs. The procedure consists in making a series of load runs, increasing the loading on successive runs and determining the arc-back rate for each.

Commercial tests on rectifiers include load runs when complete tests are required. They may be made either with the transformer being furnished for installation with the rectifier, or with a test transformer. However, such tests are costly and furthermore do not assure successful operation as regards arc-backs.

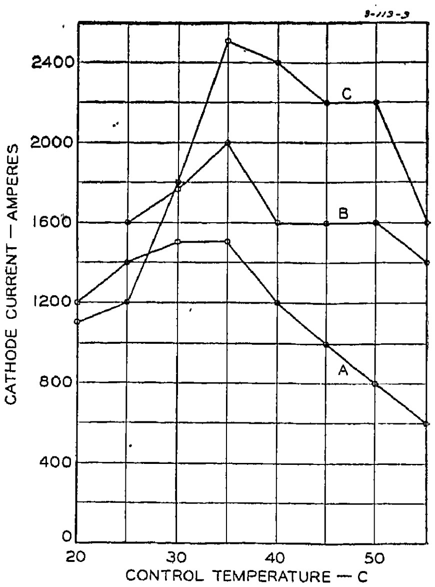


Figure 3. Results of load-limit tests on 3,000-volt multianode rectifier

Curve *A*. Continuous load limit with current increased 100 amperes every two hours

Curve *B*. 20-minute load-limit with current increased 200 amperes on successive trials; 40-minute operation at 500 amperes between peaks

Curve *C*. Five-minute load limit with current increased 200 amperes on successive trials; 25-minute operation at 500 amperes between peaks

For this reason, when a rectifier of a proved design or a large number of rectifiers of the same design are furnished and only short commercial tests are required, load runs may safely be omitted. Often load runs are made on a sampling basis, for instance, by testing every tenth rectifier.

Duty Cycles

The operating conditions incurred in electrochemical service are essentially the same as those obtained during load runs. In cases where the rectifier will operate under variable loads (as for example, railway or steel mill service), it is frequently desirable to make tests, applying a duty cycle simulating the actual operating conditions. This is particularly true where service conditions depart considerably from normal.

Faulty operation may be encountered in rectifiers at light loads as well as under overload conditions. One example of such trouble is mercury condensation on the rectifier anodes at low ambient temperatures with exposure to a cold air flow. Such condensation causes arc-backs on starting. Failures may also arise from the varying temperatures with changing load, which result in mechanical failure attributable to the movement of parts during expansion and contraction. Another difficulty which may be encountered is failure to obtain stable or constant temperature control with varying loads. Duty cycles simulating the conditions obtained in service provide a further check on the rectifier operation prior to installation, and assist in the elimination of such minor difficulties.

Determination of Rectifier Capacity

Many types of electrical apparatus are designed to meet definite temperature rise limits, for example, a 40 degree centigrade rise.[7] A principal determining factor in establishing temperature rise limits is the thermal endurance of the insulating materials, although other factors must also be considered. Since the physical deterioration of insulation under the influence of time and temperature increases very rapidly with temperature, the choice of temperature rise limit bears directly upon the probable life and reliability of the apparatus. The concept of temperature rise as a basis for rating is a convenient and useful aid in the design of most electrical apparatus.

In the case of mercury-arc rectifiers the construction is usually such that time and temperature produce no appreciable deterioration and the life is so long that replacement will probably not be required before obsolescence occurs. The principal economic considerations are operating cost and reliability. These factors cannot be gauged from any measurement of temperature rise. However, the reliability may be expressed directly in terms of the frequency of arc-back while the cost factors, such as rectifier power losses and maintenance, are also a function of the arc-back rate. Thus, it follows that rectifiers should be rated on a basis of arc-back frequency.

A high arc-back rate results in increased maintenance on protective switchgear, increased strain on transformer insulation, added hazards to operation, and interruption in service. The allowable arc-back rate depends upon the class of service. Many rectifier applications are of such character that an arc-back frequency of the order of one per month is acceptable, and 10 to 30 times this frequency for short periods does not exact any considerable economic penalty. Most railway, electrochemical, and industrial installations fall in this class. There are other applications where continuity of service is so important that an arc-back rate of more than one per year is not acceptable. Public utility installation involving essential service and industrial installations supplying power to gear cutting machines are examples of such applications. The two classes of service appear to necessitate different standards of performance.

Rectifiers having an arc-back rate of the order of one per year or less are usual at the present time for 250-volt service. Experience has shown that such performance is usually obtained when a 600-volt rectifier is operated at 250 volts. While it is possible to build rectifiers for 600-volt service having an arc-back rate not exceeding one per year, it appears that there would be certain economic penalties since the construction will entail considerable improvement in baffling with resultant arc losses, reduction in rating for a given size, and increased complexity of control.

An important consideration in the operation of a rectifier is its stability as regards arc-back. It must be recognized that an acceptable arc-back rate is not always obtained when the rectifier is first put into service. When trouble is experienced on a rectifier installation because of high arc-back frequency, the question is immediately raised, will there be progressive deterioration with an increase in arc-back frequency until the equipment becomes inoperative? Experience to date indicates that while the arc-back frequency may exceed the acceptable or desired value resulting from a large variety of causes, stable operation is generally obtained at a definite arc-back rate.

Short-Circuit Tests

Knowledge of the short-time current capacity of a rectifier is essential to the proper application of protective switchgear, and the choice of system reactance to limit fault currents. Short-circuit tests are of three general types: namely, single-cycle current limit, d-c short circuit, and simulated arc-back tests.

Current-Limit Tests

Basic data regarding the rectifier fault current capacity may be obtained by passing a single cycle of current through one anode with the d-c circuit shorted and increasing the magnitude of this current on successive trials until failure occurs. The behavior of the rectifier under these conditions is determined from oscillographic records.

The specific procedure for making current-limit tests is as follows: A single anode of the rectifier is connected to one phase of the rectifier transformer with the d-c leads short-circuited. The usual protective switchgear in a-c and d-c circuits may be provided but is arranged for delayed operation. The transformer is energized, but conduction is prevented either by negative grid voltage in the case of the multianode rectifier or by absence of ignitor excitation in the case of the ignitron. A single cycle or fault current may be caused to flow by energizing the grid or ignitor for one cycle by means of a welder control. The magnitude of the fault current is controlled by varying the phase of firing or by placing a reactance in series with the transformer circuit. In making these tests, the magnitude of fault current is gradually increased until arc-back occurs. Tests are usually made at several temperatures. Tests may also be made with more than one cycle of fault current.

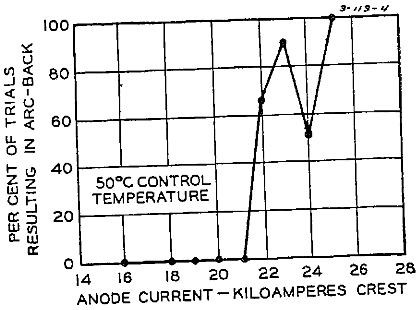


Figure 4. Results of current-limit tests on ignitron rectifier

The results of current-limit tests on a standard ignitron tank are shown in Figure 4, while Figure 5 is a typical oscillogram of a successful single cycle current-limit test.

Failure of control may occur in the course of current-limit tests as well as arc-backs. Control failures may be of various types. For example, in the case of ignitron rectifiers, the anode may fire during the cycle succeeding the conducting cycle without the ignitor being re-energized. In multianode rectifiers where voltage is applied to all the anodes but only one anode is permitted to fire, the other anodes may also fire because of failure of their grids to block. The control characteristics will be more fully described in the section on control tests.

D-C Short Circuit

D-c short-circuit tests are made for the purpose of checking

1. The ability of the rectifier to withstand the fault currents.
2. The operation of the protective switchgear.

The testing procedure consists in connecting a definite low resistance across the d-c leads of the rectifier by means of a contactor or high capacity switch. The resulting short-circuit current may be interrupted by the rectifier cathode breaker, anode breaker, or the oil circuit breaker in the transformer primary circuit. Or alternatively, the current flow may be suppressed by action of control grids or ignitors. The magnitude and duration of the short-circuit current as well as the operation of the protective switchgear may be checked by oscillograph.

The high currents flowing through the rectifier under short-circuit conditions cause the liberation of gas from the anode and other parts in the arc path. A measure of the speed of the vacuum pumps may be obtained by noting the time required to remove the gases liberated during the short circuit. Short-circuit tests also provide a check on the sturdiness of the rectifier tank and the external bus structure and their ability to withstand short-circuit stresses. While the rectifier must be able to withstand repeated short circuit without damage, it is not essential that it be able to operate without arc-back under the most severe conditions.

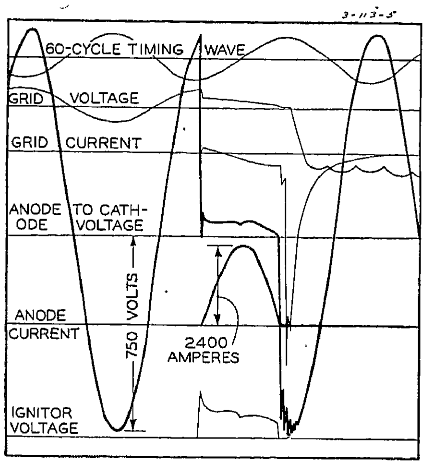


Figure 5. Typical oscillogram of successful single-cycle current-limit test (with negative voltage applied to grid to block firing on succeeding cycle)

Simulated Arc-Back

Since the switchgear furnished with rectifiers is specially selected to provide protection during arc-back it is often desirable to check its effectiveness by direct tests. This may be done by applying a short circuit between one anode of the rectifier and its cathode by means of a contactor or other shorting device, while the rectifier is operating under load. Such a short circuit simulates the conditions obtained when one anode of the rectifier arcs back. The resulting operation of the rectifier and its protective equipment is determined by taking oscillographic records.

Simulated arc-back tests may be made with the rectifier operating on either a water-box load or on a live load such as a motor generator set. In the latter case the load will feed current into the fault. The same is true where two or more rectifiers operate in parallel. Simulated arc-back tests are usually made only where complete tests are required. Because of the limitations of factory facilities, such tests are frequently made in the field.

Control Tests

The starting of anode conduction in a rectifier is controlled by the auxiliary electrodes, such as starting and excitation anodes, grids, and ignitors. In general, these may be arranged to perform the following three functions:

1. To provide the conditions required for anode conduction; namely, a source of electrons (cathode spot) at the cathode, the propagation of ionization in the mercury vapor in the arc path, and a flow of electrons to the anode. These conditions determine the pickup action.
2. To prevent the firing for one or more cycles by exerting a blocking action.
3. To delay the starting of conduction for a definite time each cycle so as to obtain phase control.

Rectifier control requirements vary from those of the simple shunt rectifier without control to those of a fully controlled rectifier with phase control for varying the output voltage and firing control for starting and stopping. The purpose of control tests is to obtain data regarding the various factors entering into the control action, and to check the effectiveness of the control means under the various conditions of operation.

Pickup Tests

Pickup tests are primarily of value in determining the excitation requirements of the rectifier and checking its light load operation. Pickup tests may be made by operating the rectifier at full voltage on a high resistance or counter electromotive force load. The pickup voltage is determined by varying the load resistance or the value of counter electromotive force voltage and observing the d-c volt-ampere characteristics of the rectifier.

In multianode rectifiers the arc is established at the beginning of operation by means of the starting anode and maintained during operation by the excitation anodes which are usually continuously excited. The excitation arc must provide sufficient ionization to assure pickup of each of the main anodes when its voltage becomes positive. When the rectifier is equipped with grids, the grid may also be excited so as to assist anode pickup. In ignitron rectifiers the arc is established during each cycle by means of the ignitor. The ignition current must both initiate the cathode spot and provide sufficient ionization to pick up the anode. As in the case of multianode rectifiers, the pickup characteristics are dependent upon the grid excitation.

The voltage range of the auxiliary power supply is an important consideration in the determination of rectifier excitation requirements. Low voltage is frequently encountered in normal operating service. Sufficient excitation must be provided to obtain satisfactory pickup at the minimum expected auxiliary voltage.

Poor pickup characteristics may result in a variety of troubles. Where several rectifiers operate in parallel and one rectifier is taken out of service, difficulty may be experienced in causing it to pick up and share load with the other rectifiers when it is reconnected to the bus. When rectifiers are installed for automatic operation, the control relay is sometimes arranged so that its action is dependent upon the pickup of the rectifier. In supervisory operation reliable pickup is essential to good operation.

The pickup characteristics of a rectifier may be influenced considerably by the condition of the rectifier tank. For example, it has been found that rectifiers which picked up satisfactorily when new gave trouble as operation was continued and the tanks became freer from gas because of aging. Also, the control temperature and the temperature distribution over the rectifier tank may have an important bearing on the pickup behavior. In multianode rectifiers, poor pickup may result from leakage resistance between rectifier tank and negative ground since the rectifier tank acts as a grid and tends to block the firing of the main anode.

Blocking Tests

Blocking action may be applied under two conditions. In one case it is used to prevent conduction while the rectifier is being placed in service. In the other case it is used to interrupt the rectifying action either at normal loads or under fault conditions. Effective blocking action is more difficult to obtain in the latter case, as the ionization carries over from the conducting cycle.

The minimum blocking voltage required on the grids of a rectifier in order to prevent conduction during starting may be measured by applying a positive d-c voltage to the anodes and a variable negative d-c voltage to the grids. The voltage on the grid is varied until the anode fires. This type of test may be made on either single-anode or multianode rectifiers. A typical grid characteristic for an ignitron is shown on Figure 6.

The effectiveness of the blocking action in interrupting the rectifier current under fault conditions may be determined by means of the same procedure used in making current-limit tests.

Phase-Control Tests

The output voltage of a rectifier may be controlled by varying the phase of the voltage applied to the grids or ignitors. For proper performance of this control function it is essential that a definite relation be accurately maintained between the control voltage applied to grid or ignitor and the anode firing.

There are two possible causes of control failure, namely, loss of control or early firing of the anode, and delay in anode pickup. Some of the factors which may cause control failure are: high load current, high or low temperature, foreign gases, faulty construction. In order to determine the grid or ignitor control characteristics it is necessary to operate the rectifier on load under the various conditions and observe or record the voltage and current characteristics of the control electrode and the anode by means of an oscillograph.

Loss of control is incurred when the anode fires either before or without the application of a control impulse. This may occur in a multianode rectifier when the grid does not prevent ionization reaching the anode and so permits the anode to fire when it becomes positive. In ignitron rectifiers the positive anode voltage acting on the residual ionization may initiate a cathode spot before the ignitor is energized.

A variable time may exist between the firing of the grid and the main anode under certain conditions. This phenomenon has been noted on both multianode and ignitron rectifiers. The difference in time of firing may be reduced to a negligible amount or eliminated by proper choice of firing circuit constants and operating conditions.

The control characteristics of the rectifier unit may be checked by making voltage-regulation tests with specified phase-control settings and comparing the measured output voltages against the calculated values.

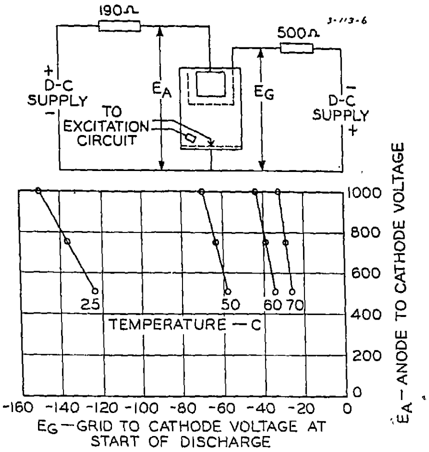


Figure 6. Grid-blocking characteristic of ignitron rectifier

Performance Characteristics

The performance characteristics of a rectifier unit may be determined by direct measurement. However, such tests require the assembly of the complete equipment and present other practical difficulties. The performance characteristics are, therefore, ordinarily determined from calculations based on measurements of rectifier and transformer losses and transformer reactance.

Efficiency

The "directly measured efficiency" is obtained from simultaneous measurements of input and output power. Elaborate precautions must be taken in order to obtain accurate results using this method.[8]

The "conventional efficiency" is obtained from the component losses determined from loss tests. Since most rectifiers have a high efficiency and the losses are only a small part of the total power, a given error in the measurement of the losses does not affect the efficiency as much as the same percentage error in the measurement of total input and output. For this reason the conventional efficiency method is preferred.

The losses incurred in a rectifier equipment are classified as follows:

(*a*). Rectifier arc-drop loss.

(*b*). Rectifier auxiliary losses.

(*c*). Rectifier transformer losses.

Methods for measuring these losses follow:

Arc-Drop Loss

Various methods have been proposed for the measurement of the rectifier arc-drop loss. See the report on AIEE Standard 6.[5] Of these, the oscillographic method has found widest acceptance. However, this method is open to the objection that the rectifier arc losses when operating at low voltage differ from those obtained in full voltage operation since the anode current wave forms are not the same. In order to overcome this objection an improved oscillographic method has been devised which permits operation of the rectifier at full voltage during arc-drop measurements.

The improved oscillographic method employs a noninductive external resistor connected in series with the oscillograph element and a small mercury-arc-rectifier tube (or dry-type rectifier) in shunt with the oscillograph element as shown in Figure 7. The shunting rectifier is connected so as to limit the voltage across the oscillograph element during the inverse period. This voltage may be further reduced by means of a battery connected in series with the shunting rectifier.

A typical arc-drop oscillogram of a large ignitron is shown in Figure 8. This oscillogram shows the arc-drop voltage and the anode current. The anode current wave is used to determine the limits of the conduction period. On large pumped rectifiers, there is considerable inductive effect in the rectifier tank itself which results in an inductive voltage during the commutation periods. This inductive voltage is superimposed on the actual arc-drop voltage so that the limits of the conduction period cannot be determined directly from the arc-drop voltage wave.

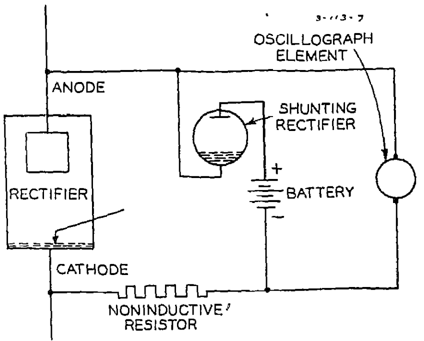


Figure 7. Schematic diagram of oscillograph circuit for measuring rectifier arc drop

The average arc-drop voltage is obtained from the oscillogram by determining the average ordinate of the segment of the arc-drop voltage wave within the limits of the conduction period by means of a planimeter. The arc-drop loss is equal to the product of this voltage multiplied by the total cathode current measured simultaneously with the taking of the oscillogram.

On small rectifiers where inductive effects are negligible and the arc-drop voltage wave is essentially flat, the average arc-drop voltage may be measured directly on a d-c voltmeter by using a comparison method. In this method the arc-drop voltage wave is observed on a cathode-ray oscillograph, and the amplification is adjusted so as to obtain a suitable deflection on the screen. The oscillograph element is then switched to a calibrating circuit consisting of a potentiometer with a d-c voltmeter. The oscillograph element is connected to the voltmeter terminals, and the d-c voltage drop is adjusted until the same deflection is obtained on the oscillograph as when viewing the arc-drop voltage.

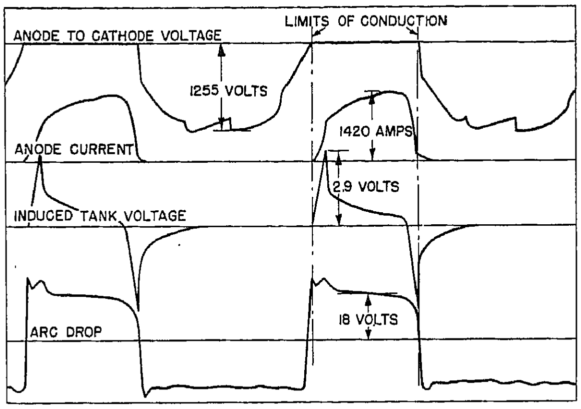


Figure 8. Typical oscillogram of arc drop in ignitron rectifier

Inductive voltage measured by passing anode current through duplicate tank having copper bus connecting anode direct to bottom of tank in mercury pool

The oscillographic method of measuring the arc drop in rectifiers has one advantage over other methods, in that it permits observation of the rectifying action during full voltage operation. This is useful in the study of rectifier action in the course of developmental or field tests.

Auxiliary Losses

The auxiliary losses are determined by measuring the input power to the auxiliaries.

Rectifier Transformer Losses

No-Load Loss Tests. Tests for no-load losses may be made in accordance with the standard methods used for other transformers.

Load Loss Tests. The load losses for rectifier transformers may be determined by several methods. One of these is described in the report on AIEE Standard 6.[5] This method requires tests with two or more connections for some circuits. An alternative method which requires only a single connection is described in Appendix A.

Voltage Regulation

As in the case of efficiency, the voltage regulation of a rectifier unit may be determined from direct measurement. The procedure consists in measuring the output voltage at various values of output current with a constant voltage applied to the primary winding of the transformer. Since the reactance of the supply system effects the apparent regulation of the rectifier unit, the regulation cannot be determined accurately by direct measurement. Therefore, the regulation is ordinarily determined from values of output voltage calculated by subtracting the voltage drops in the rectifier and transformer from the rectifier no-load voltage. The method of calculating the output voltage is described in Appendix B.

Power Factor

The power factor of a rectifier unit may be determined from simultaneous measurements of the input power and input volt-amperes. The reactance of the supply system affects the power factor in the same manner as it does the regulation. For this reason direct measurement of power factor under test conditions must be considered approximate. The power factor is generally determined by calculations based on separately measured characteristics of the rectifier and transformer such as the transformer reactance and magnetizing current.

Wave Form

The harmonic composition of the rectifier input and output waves may be determined either by direct harmonic measurements or from analyses of oscillograms. The over-all effect of all the harmonics may also be determined from telephone-influence-factor measurements The wave form of a rectifier is of importance primarily as one of the factors of which a knowledge is desirable in evaluating the influence of the power system on neighboring communication circuits. Since the harmonics produced by a rectifier are affected by the reactance of both the a-c and d-c power circuits to which the rectifier is connected, measurements made under test conditions in the factory may differ from those obtained in the field under actual operating conditions because of the different constants in the two circuits. Methods are available for calculating with reasonable accuracy the wave shape of the voltage and the current in both the a-c and d-c sides of a given rectifier installation when the circuit constants are known.[9]

Testing Facilities

Extensive facilities are required for the testing of mercury-arc rectifiers. There must be provided a complete power system capable of supplying and loading the rectifier to the limit of its capacity, an auxiliary power supply together with the essential rectifier auxiliaries, and an extensive assortment of both standard and special instruments. An arrangement of the rectifier test equipment is shown in Figure 9.

The a-c power system used for testing rectifiers must be of sufficient capacity to maintain substantially sinusoidal wave shape at full rectifier loading and also to give short-circuit currents approximating those obtained in service. Inasmuch as rectifiers must be operated at and above their rated loads for considerable periods during the course of tests, motor generator sets arranged to pump back into the

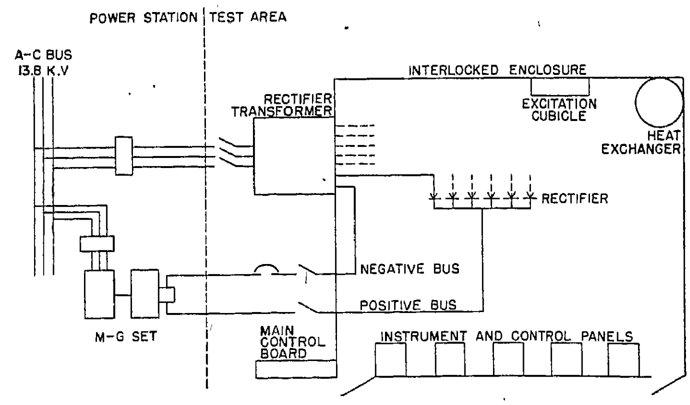


Figure 9. Arrangement of rectifier test equipment

a-c system are used for absorbing the rectifier output.

The generous use of oscillographic instruments is essential in rectifier testing. Oscillographic equipment must include both magnetic and cathode-ray types and must provide facilities for both visual observation and photographic recording. Special instruments and special instrument techniques have been developed for use in rectifier testing. Of these perhaps the most important are the arc-back indicators, the memory oscillograph, and the technique for measuring arc drop. The full use of all available instruments and the development of new instruments is essential to further progress in the analysis of rectifier action.

Appendix A. Transformer Load Loss

The transformer losses may be determined from measurements made by short-circuiting all secondary windings and passing rated sinusoidal current at rated frequency into the primary windings. The losses absorbed by the transformer during this test represent the full-load copper losses when the current is sinusoidal. The total copper losses when operating with nonsinusoidal current in rectifier service may be calculated from this test value as follows:

Symbols

The symbols used in the formulas are defined as follows:

I_p = rated primary current
I_s = rated secondary current

(These primary and secondary currents are the rms values of current for a flat top wave without overlapping.)

R_p = ohmic resistance of primary winding
R_s = ohmic resistance of secondary winding

(These resistances are for copper temperature of 75 degrees centigrade.)

K_p = rated kilovolt-amperes of primary windings
K_s = rated kilovolt-amperes of secondary windings
n = turn ratio
P_1 = losses absorbed by transformer during test
P_2 = ohmic losses for sinusoidal current
P_3 = stray load losses for sinusoidal current
P_4 = ohmic losses for rectangular current
P_5 = total load losses for rectangular current

Calculation of Stray Load Losses

The losses absorbed by the transformer during the test, P_1, should be corrected to a temperature of 75 degrees centigrade.

The current in the short-circuited secondary winding of the transformer during the test will be $I_s' = nI_p$ with rated sinusoidal current of value I_p flowing in the primary winding. The ohmic losses for sinusoidal currents of these values, P_2, are:

$$P_2 = (I_p)^2R_p + (I_s')^2R_s = I_p^2(R_p + n^2R_s)$$

The stray load losses for sinusoidal currents, P_3, are then:

$$P_3 = P_1 - P_2$$

It is assumed that these stray load losses are the same whether the current of the transformer is sinusoidal or has the rectangular wave form obtained during the normal operation of the rectifier. This assumption is justified by a large number of tests carried out by different methods, the variations not being larger with this method than with other methods. With this method, however, it is assumed that the requirements of the various windings of the transformers as to symmetry are entirely fulfilled.

Calculation of Total Load Losses

The ohmic losses for rectangular current, P_4, which are obtained during normal service of the rectifier are:

$$P_4 = I_p^2R_p + I_s^2R_s$$

The total load losses of the transformer during normal service of the rectifier, P_5, are equal to the sum of the ohmic and stray load losses and:

$$P_5 = P_3 + P_4 = P_1 + I_s^2R_s\left[1 - \left(\frac{I_s'}{I_s}\right)^2\right]$$
$$= P_1 + I_s^2R_s\left[1 - \left(\frac{K_p}{K_s}\right)^2\right]$$

When making load-loss tests on the rectifier transformer, the interphase transformers and anode reactors should be disconnected.

For interphase transformers in which the load current is almost entirely direct current, the d-c resistance of the winding may be used in computing the load loss.

Appendix B. Rectifier Output Voltage

The formula for the calculation of the d-c voltage of the rectifier unit at a specified load current is:

$$E_d = E_{d0} - E_x - E_r - E_a$$

where

E_d = d-c voltage at the specified load current
E_{d0} = theoretical d-c voltage (This is the voltage which would be obtained at no-load with no phase control. It is the voltage obtained at the intersection of the regulation curve projected to the zero load line, neglecting the increase in voltage at light load.)
E_x = commutating reactance voltage drop
E_r = resistance voltage drop
E_a = arc voltage drop

The theoretical d-c voltage and the voltage drops may be calculated by using the following formulas:

Theoretical d-c voltage $E_{d0} = \sqrt{2}E_s\frac{P}{\pi}\sin\frac{\pi}{P}$

Commutating reactance voltage drop

$$E_x = \frac{I_cX_cP}{2\pi}$$

Resistance voltage drop $E_r = \frac{W}{I_d}$

in which

E_s = secondary line to neutral voltage of rectifier transformer (rms value)
P = number of phases in simple rectifier
I_c = direct current commutated
X_c = commutating reactance in ohms (line to neutral)
W = load losses of main and interphase transformers
I_d = d-c load current

The commutating reactance X_c, attributable to the main transformer, may be measured by short-circuiting the primary windings and passing sinusoidal current at rated frequency between two secondary anode leads between which a transfer of current occurs during commutation. The commutating reactance anode-to-neutral is obtained by dividing one half of the measured impedance voltage by the applied current.

The transformer load loss W may be determined by the method described in Appendix A.

The method for determining the arc-drop voltage E_a is described in the section on efficiency tests.

References

1. ELECTRICAL DISCHARGES IN GASES, Karl T. Compton, Irving Langmuir. *Reviews of Modern Physics*, volume 2, number 2, April 1930, pages 123–242; volume 3, number 2, April 1931, pages 191–257.

2. THE RELATION OF RESIDUAL IONIZATION TO ARC-BACK IN THYRATRONS, K. H. Kingdon, E. J. Lawton. *General Electric Review*, volume 42, number 11, November 1939, pages 474–8.

3. THE PHASE OF ARC-BACK, Albert W. Hull, Frank R. Elder. *Journal of Applied Physics*, volume 13, number 3, March 1942, pages 171–8.

4. BACKFIRES IN MERCURY-ARC RECTIFIERS, J. Slepian, L. R. Ludwig. AIEE TRANSACTIONS, volume 51, 1932, pages 92–104.

5. ACCEPTANCE TESTS FOR METAL-TANK MERCURY-ARC RECTIFIERS. Report on AIEE Standard 6, June 1934.

6. MERCURY-ARC-RECTIFIER RESEARCH, Albert W. Hull, H. D. Brown. AIEE TRANSACTIONS, volume 50, 1931, pages 744–56.

7. GENERAL PRINCIPLES UPON WHICH TEMPERATURE LIMITS ARE BASED IN THE RATING OF ELECTRICAL MACHINERY AND APPARATUS. AIEE Standard 1, June 1940.

8. WATT-HOUR-METER PERFORMANCE WITH POWER RECTIFIERS, C. T. Weller, H. E. Trekell, F. O. Stebbins. AIEE TRANSACTIONS, volume 59, 1940, August section, pages 449–57.

9. RECTIFIER WAVE SHAPE. Edison Electric Institute publication *EI*, April 1937.

A New-Type Carrier Relay Protection

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THE Hydro-Electric Power Commission of Ontario and the Beauharnois Light, Heat, and Power Company have placed in service recently a new type of power-line-carrier equipment. Channels for high-class interterminal communication are combined with other channels which provide a transfer of impulses for use with the relaying on transmission lines, giving a desired improvement in the speed of operation.

Stability Problem

The 220-kv 25-cycle system of the Hydro-Electric Power Commission is shown in Figure 1, and has been described in the AIEE Transactions.[1] In the earlier paper a record of experience as to line faults was presented and analyzed. The problem of relaying in relation to transient stability was discussed. It was pointed out that studies on an a-c network calculator and operating experience indicated that, in order to maintain stability at the normal line loadings of 150,000 to 170,000 kw per main line, it would be necessary not only to clear two-phase-to-ground and three-phase faults at the line terminal nearest to the fault in 0.2 second approximately but also to open the breakers at the remote terminal simultaneously or in 0.25 or 0.3 second at the most. If this could be accomplished, transient stability would not be a problem on this system which carries an important industrial load of 600,000 to 650,000 kw in large centers of population.

The original basic scheme of phase relaying on the four main 220-kv trunk lines, as stated in the earlier paper, consists of directioned two-stage impedance relays of a conventional type. The first stage operates in one cycle (0.04 second) or less for all faults within the 80 to 90 per cent of the line adjacent to the relays. The second range, covering the whole line and overlapping into the contiguous line sections, is set to operate in 12 to 20 cycles (0.5 to 0.8 second), thus being time selective with the instantaneous relays on these sections. This basic relaying scheme has proved very successful during the development period of the system when loads were lighter; but with present loadings it will be seen that it does not meet the requirements for stability, because clearance is delayed too long at the remote terminal for faults located within the 10 to 20 per cent section at the end of a line. It will be noted also that the line from Beauharnois to Chats Falls is tapped for the infeed from the MacLaren-Quebec Power Company, thus presenting a problem in distance relaying. Moreover, there are two short line sections which have impedances at 25 cycles that are too low for best results with plain distance relaying.

Regarding the long lines, there was doubt as to the desirability of depending on the transmission of a blocking signal to prevent incorrect tripping. Therefore, a carrier relaying system was proposed which could be applied to any or all types of impedance relays without changing their normal use. Since the carrier relaying feature should be supplementary to the original relaying system, the failure of the carrier link should not in any way affect the normal operation of the distance impedance relays which use time delay to select the faulted line. The carrier feature should provide only a speed-up in the relay action.

When the first stage distance relays are set to operate for faults which are within the 80 to 90 per cent of the line adjacent to the relays, the carrier protection is unnecessary except for faults which are in the 10 to 20 per cent sections at the ends of the line. The latter are seldom more than 20 per cent of the total number of line faults. Therefore, the addition of carrier as a protection or improvement in the protection on any transmission system should be balanced against this 20 per cent of the faults. If the carrier should fail to operate for any cause whatsoever, only 20 per cent of the total system faults would be affected adversely, and in no case would the incorrect operation or failure in the carrier system be detrimental to the relay system because failure only increases the clearing time for faults in the end sections of the line.

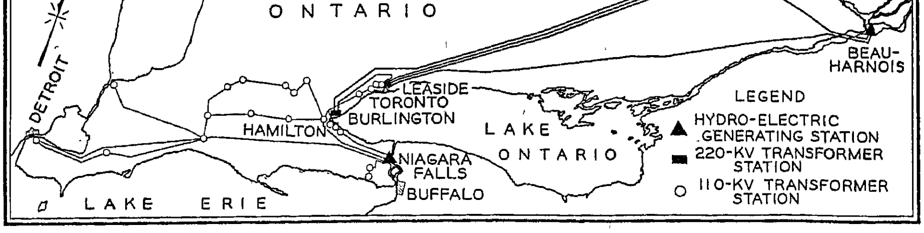
There existed also the problem of communication between the Beauharnois terminal and the commission's plants at Chats Falls and Leaside. Paugan, Chats Falls, Leaside, and stations of the 110-kv system had been interlinked by the commission's overhead telephone lines, but no such provision had been made for MacLaren or Beauharnois. It was desired to link these terminals, particularly the large source at Beauharnois, with the rest of the system by a suitable communication channel which could be of the power-line-carrier type.

Requirements of Carrier System

The relay system should use the directioned instantaneous relay, which protects most of the line, to set up the trip circuit for its own breakers. At the same time this operation would cause an impulse to be sent to the remote end to by-pass the timer on the second impedance range, thereby giving clearances at both ends of the line which are sufficiently near to simultaneous operation to meet the stability requirements. In this scheme, it is not necessary to depend on the passage of the carrier impulse to prevent tripping on external faults; therefore, it would be inherently safer and preferable to the blocking scheme.

The impulse for relaying purposes on one line might be produced by a single frequency or tone in the audio range,

Figure 1. High-voltage lines in the 25-cycle system



Paper 43-147, recommended by the AIEE committee on protective devices and joint subcommittee on power system applications of carrier current for presentation at the AIEE national technical meeting, Salt Lake City, Utah, September 2-4, 1943. Manuscript submitted June 16, 1943; made available for printing July 16, 1943.

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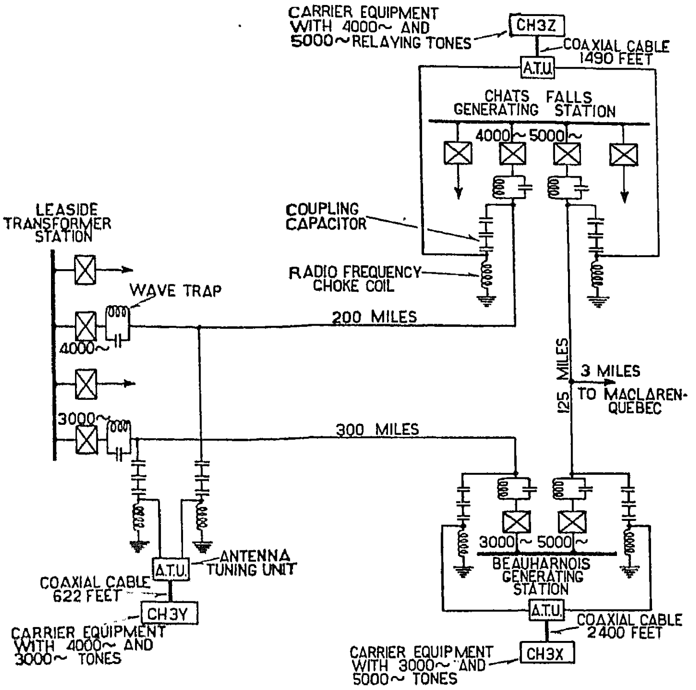
CARRIER EQUIPMENT WITH 4000~ AND 5000~ RELAYING TONES
CH3Z
COAXIAL CABLE 1490 FEET
A.T.U.
CHATS FALLS GENERATING STATION
4000~ 5000~
LEASIDE TRANSFORMER STATION
COUPLING CAPACITOR
RADIO FREQUENCY CHOKE COIL
WAVE TRAP
4000~
200 MILES
125 MILES
3 MILES TO MACLAREN-QUEBEC
3000~
300 MILES
ANTENNA TUNING UNIT
A.T.U.
COAXIAL CABLE 622 FEET
CH3Y
CARRIER EQUIPMENT WITH 4000~ AND 3000~ TONES
3000~ 5000~
BEAUHARNOIS GENERATING STATION
A.T.U.
COAXIAL CABLE 2400 FEET
CARRIER EQUIPMENT WITH 3000~ AND 5000~ TONES
CH3X

Figure 2. Arrangement of carrier equipment and tones for relaying

which is used for amplitude modulation of the carrier. A separate tone would be assigned to each high-voltage line. One tone generator for transmitting and one tone filter for receiving are required at each end of a protected line, because the impulse for use with the protective relays might originate at either end of the line. An economical design is possible, when only the tone generators and filters which are required are installed at each station. The number of tones which can be used and their place in the frequency spectrum are determined by the sharpness of cutoff in the band-pass filters. Three tones of 3,000, 4,000, and 5,000 cycles were chosen and assigned to the lines, as shown in Figure 2.

The usefulness of this method of carrier relaying depends on whether or not the impulse can be transmitted past the line fault. Tests on lines with grounded conductors showed that, with suitable carrier sets, the impulse could be sent past a ground on one or two wires of the circuit and probably past arcing grounds on all three wires.

Investigation showed that equipment could be obtained wherein both the desired functions of communication and relaying could be combined. Moreover, one set could be coupled to two transmission lines at each terminal, so that one set could be used to serve two lines. It was decided to proceed with a trial installation which would provide communication with Beauharnois and an improvement in the relaying on three lines shown in Figure 2, with the intention that if operation is successful, similar equipment will be installed on the other long lines. The sets and the installation are described in more detail in a later part of this paper.

Auxiliary and Out-of-Step Relays

A schematic diagram of the relay system at one end of the transmission system is shown in Figure 3. A description of the standard relay system for protection of a transmission line is unnecessary. However, the additional relays which are shown in this diagram will be described in the paragraphs hereinafter.

The out-of-step relays are standard "slug" relays of the telephone type, which are equipped with a sealed mercury contact for opening a circuit after a time delay. These operate in the following man-

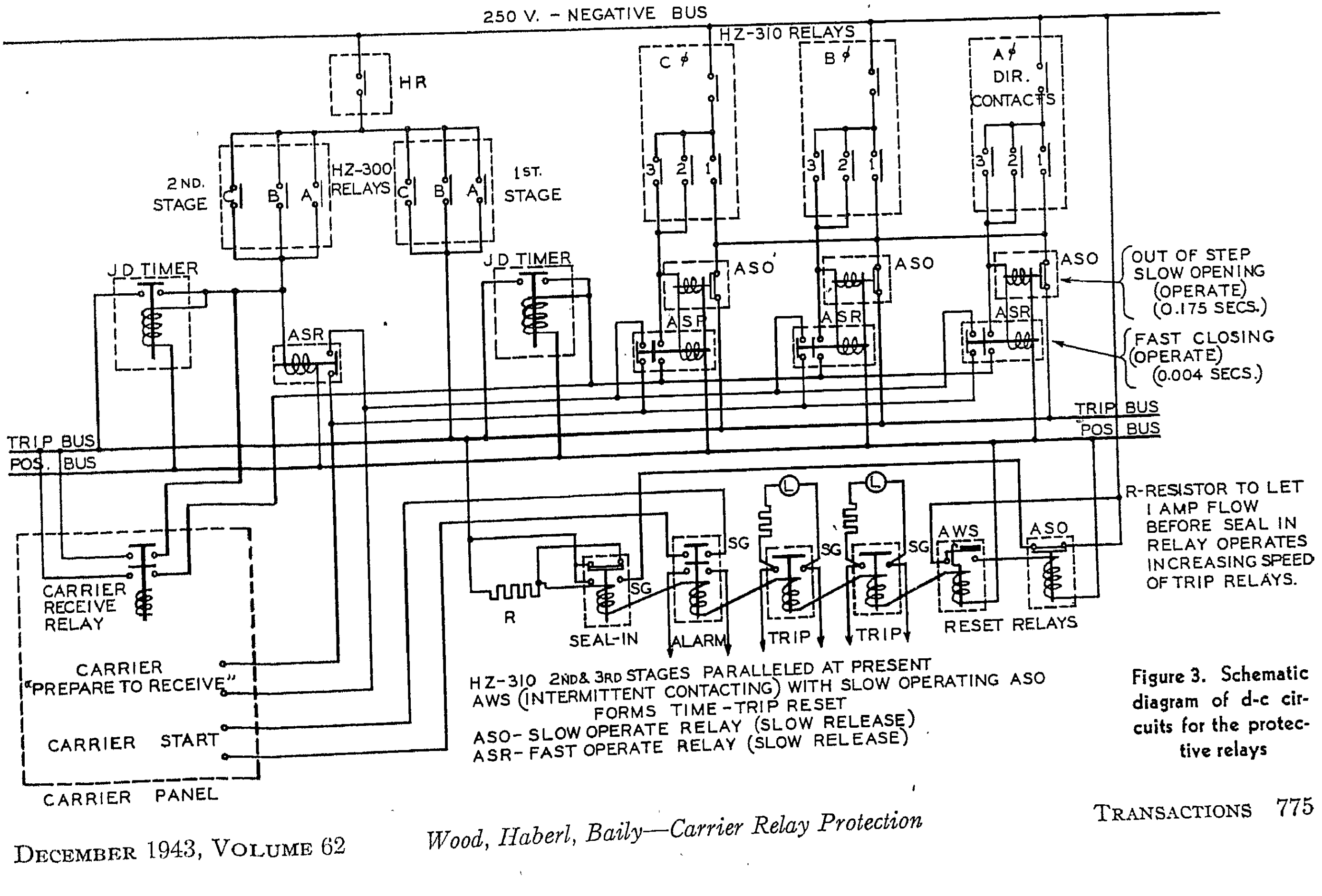
250 V. - NEGATIVE BUS
HR
HZ-310 RELAYS
C ϕ
B ϕ
A ϕ DIR. CONTACTS
3 2 1
2ND. STAGE
C B A
HZ-300 RELAYS
1ST. STAGE
J.D. TIMER
ASR
ASO
OUT OF STEP SLOW OPENING (OPERATE) (0.175 SECS.)
FAST CLOSING (OPERATE) (0.004 SECS.)
TRIP BUS
POS. BUS
R-RESISTOR TO LET 1 AMP FLOW BEFORE SEAL IN RELAY OPERATES INCREASING SPEED OF TRIP RELAYS.
CARRIER RECEIVE RELAY
R
SG
L
SEAL-IN
ALARM
TRIP
TRIP
AWS
RESET RELAYS
CARRIER "PREPARE TO RECEIVE"
CARRIER START
CARRIER PANEL
HZ-310 2ND & 3RD STAGES PARALLELED AT PRESENT
AWS (INTERMITTENT CONTACTING) WITH SLOW OPERATING ASO FORMS TIME-TRIP RESET
ASO- SLOW OPERATE RELAY (SLOW RELEASE)
ASR- FAST OPERATE RELAY (SLOW RELEASE)

Figure 3. Schematic diagram of d-c circuits for the protective relays

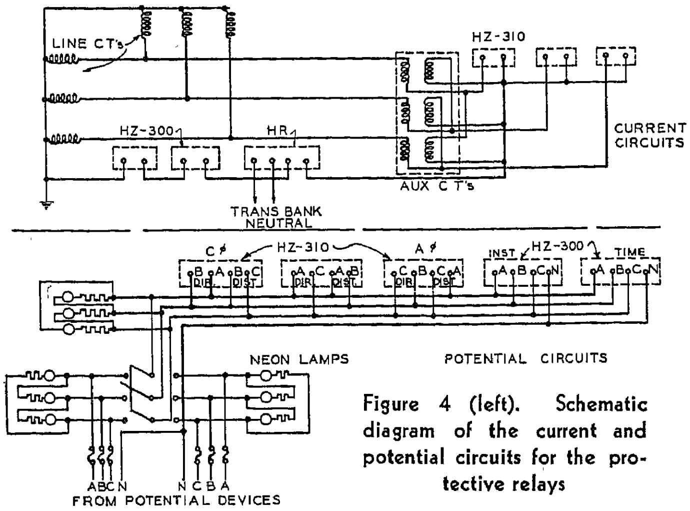

Figure 4 (left). Schematic diagram of the current and potential circuits for the protective relays

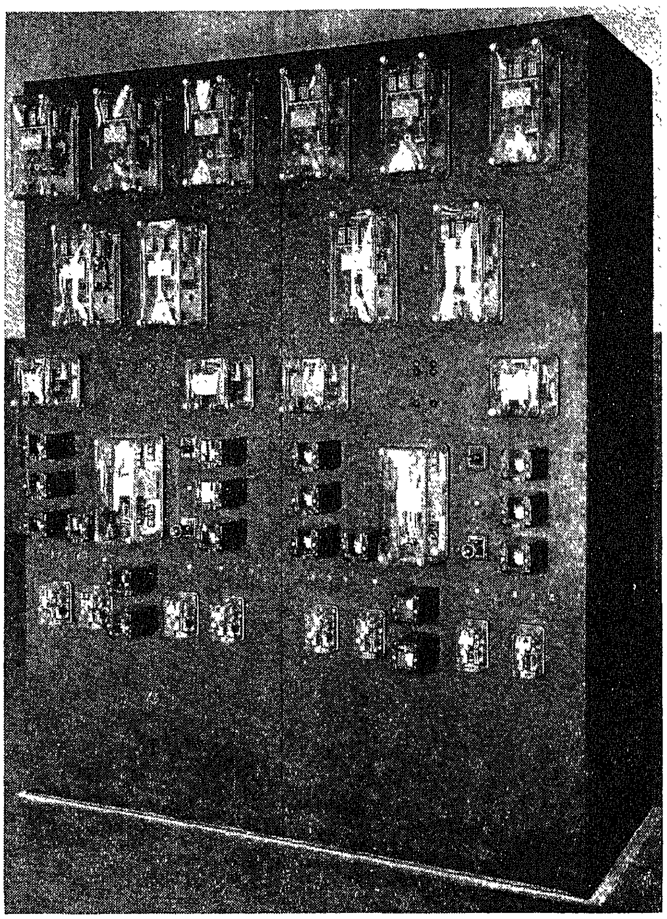

Figure 5. Front view of panel showing the protective relays

ner: If the system should become unstable, all three of the second-stage impedance relays will operate during the swing, and open the trip circuit of the first-stage impedance relays. If the system should remain out of step, the second-stage relays would permit tripping ultimately through the contacts of the *JD* timer. However, if the system should swing back, the first-stage impedance relays will reset, followed by the second-stage impedance relays and the type *ASO* relays, provided the *JD* timer has not closed its contacts. If the out-of-step condition progresses or revolves faster than the time setting of the *ASO* relays, the trip circuit will be closed through the contacts of the first-stage impedance relays. The operation of the first-stage relays on faults within the 80 to 90 per cent section of the line is not affected in any way, as the trip circuit would be closed prior to the operation of the *ASO* relays.

The auxiliary relays type *ASR* are similar to the type *ASO*, except that their action is not delayed. Their function is twofold: first, they operate the *JD* timer, and second, they prepare the carrier set for reception of the tone or impulse, if the carrier equipment happens to be in use for transmitting a telephone conversation at the time.

The type *ASO* and *AWS* relays for resetting the trip circuit are similar to the relays described heretofore, and their function is merely to keep the trip-circuit type *SG* relays in the closed position for a predetermined time, during which the carrier transmitter is started and the appropriate relaying tone is transmitted to the remote station. This time delay must be sufficiently long to trip the local breakers properly and to permit the reception of the relaying tone at the far end of the line where the contacts on the timer for the second-stage impedance relays must be by-passed long enough to permit tripping the breakers.

The current and potential circuits for the protective relays are shown in Figure 4, which contains some points of interest. Delta connection of the neon lamps was necessary because they would not always "strike" on the phase-to-neutral voltage of approximately 60 volts. Star-connected current transformers are used as the source for both the phase-to-neutral impedance relays and the phase-to-phase impedance relays. The zig-zag connection of the auxiliary current transformers has some advantages: first, it allows the use of ground relays while maintaining delta equivalent currents in the phase relays, and second, the volt-ampere burden on the main current transformers is less than straight isolating auxiliary transformers.

Figure 5 shows a general view of the power-line protective-relay panels. At the top of the panels are the phase relays. The ground relays are next, and below these are the timing relays. The type *HR* directioned ground-current relays are in the center of the panels farther down. The type *ASO* relays for out-of-step operation are on the right of the type *HR* relay, and the "prepare-to-receive" relays type *ASR* are at the left. The bottom line of relays are the "trip", "carrier start", "alarm," and "seal-in" relays. The neon lamps are without covers. The telephone-type lamps are used for trip-circuit supervision.

Carrier Equipment

The carrier equipment at each terminal consists of the following units: the carrier transmitter which is connected by a carrier frequency transmission cable to the line tuning unit, the coupling capacitor by which carrier is impressed on each line, the line trap which isolates the carrier currents from the power frequency switching equipment and transformers, and the control unit which includes the carrier receivers.

Transmitter

The transmitter is capable of delivering a maximum of approximately 400 watts of carrier energy, which can be modulated at least 90 per cent, into a load of nominal value of 70 ohms. This is the characteristic impedance of the carrier frequency transmission cable employed to transmit the energy to the line tuning unit and thence to the coupling capacitor and the high-voltage line. The carrier frequency range is 145 to 205 kilocycles. Low temperature-drift quartz crystals in duplicate are employed as the primary source of carrier energy.

The carrier circuits consist essentially of a crystal oscillator followed by a low-power buffer amplifier, a driver amplifier, an intermediate power amplifier, and a final power amplifier. All circuits are tuned with the exception of the buffer amplifier, which is impedance coupled to the driver. The driver may be connected as a master oscillator by a single switching operation. The output circuits are designed to couple to a transmission cable or other nonreactive load of approximately 70 ohms and to pass the sidebands represented by modulating frequencies up to 6,000 cycles per second without serious attenuation.

The power output of the transmitter may be reduced to a minimum of approximately 15 watts. Power reduction is accomplished in three coarse steps corresponding to approximately 165, 50, and 15 watts. The method used consists essentially of inserting resistance in the plate circuit of the final amplifier stage, thus reducing the net plate voltage. The primary reason for thus reducing the power is to minimize interference on other services, since, under normal conditions and for short distances, the full power is not required for telephony. However, the power must be raised to its full value when a signalling tone is to be transmitted, so a contactor is employed to shunt the resistance out when signalling is required.

The audio-frequency system consists of an input amplifier stage, a driver stage, and a push–pull modulator stage. The system was specially designed to give an audio characteristic which is substantially uniform from 100 to 6,000 cycles. The total harmonic distortion with full carrier power and 90 per cent modulation depth is less than five per cent. The carrier noise is more than 45 decibels below 90 per cent modulation.

Included in the transmitter is a changeover relay actuated primarily by the voice-operated devices and the tone signalling circuits. This relay transfers the output circuits from "received" to "transmit", besides controlling the carrier. The carrier transmission is started by applying plate voltage to one of the lower stages. In the "stand-by" condition, all other plate voltages remain on, and the anode currents are reduced by appropriate grid bias voltages. The crystal stage is allowed to oscillate continuously, as crystals on these carrier frequencies are often slow in starting. By the use of suitable shields and filters, any pickup in the associated receiver is avoided.

Control Unit

The control cabinet includes the following units: two carrier receivers, a receiving amplifier containing the voice-operated disabling device, and an amplifier for the calling loudspeaker, a transmitting amplifier containing the voice-operated carrier-control device and calling oscillator, two low-pass filters, two tone generators, two filter amplifiers, and a calling unit containing the hybrid coil, auxiliary relays, and microphone supply.

Each of the carrier receivers consists of a seven-tube unit, designed for the reception of frequencies between 140 and 200 kilocycles, and comprising a tuned radio-frequency amplifier controlled by an amplified automatic volume control circuit. The input circuits to the receivers include two wave traps and an input attenuator, together with a muting relay which blocks the receiver while the transmitter is operating. This relay is controlled by contacts on the antenna changeover relay, in such a way as to close the contacts on the muting relay before the transmission of carrier begins, and thereby eliminate at the receiver output any pickup from the carrier. This pickup is quite appreciable and is due to a large extent to the capacity effects at the antenna changeover relay. The wave traps are incorporated to provide mitigation of adjacent channel interference. The input attenuator provides a coarse adjustment of carrier input level to ensure that the normal incoming carrier will work the *AVC* circuits properly and to assist in overcoming the effects of noise. Since the noise level is relatively high, the receiver is relatively insensitive, requiring a minimum input of 45 millivolts

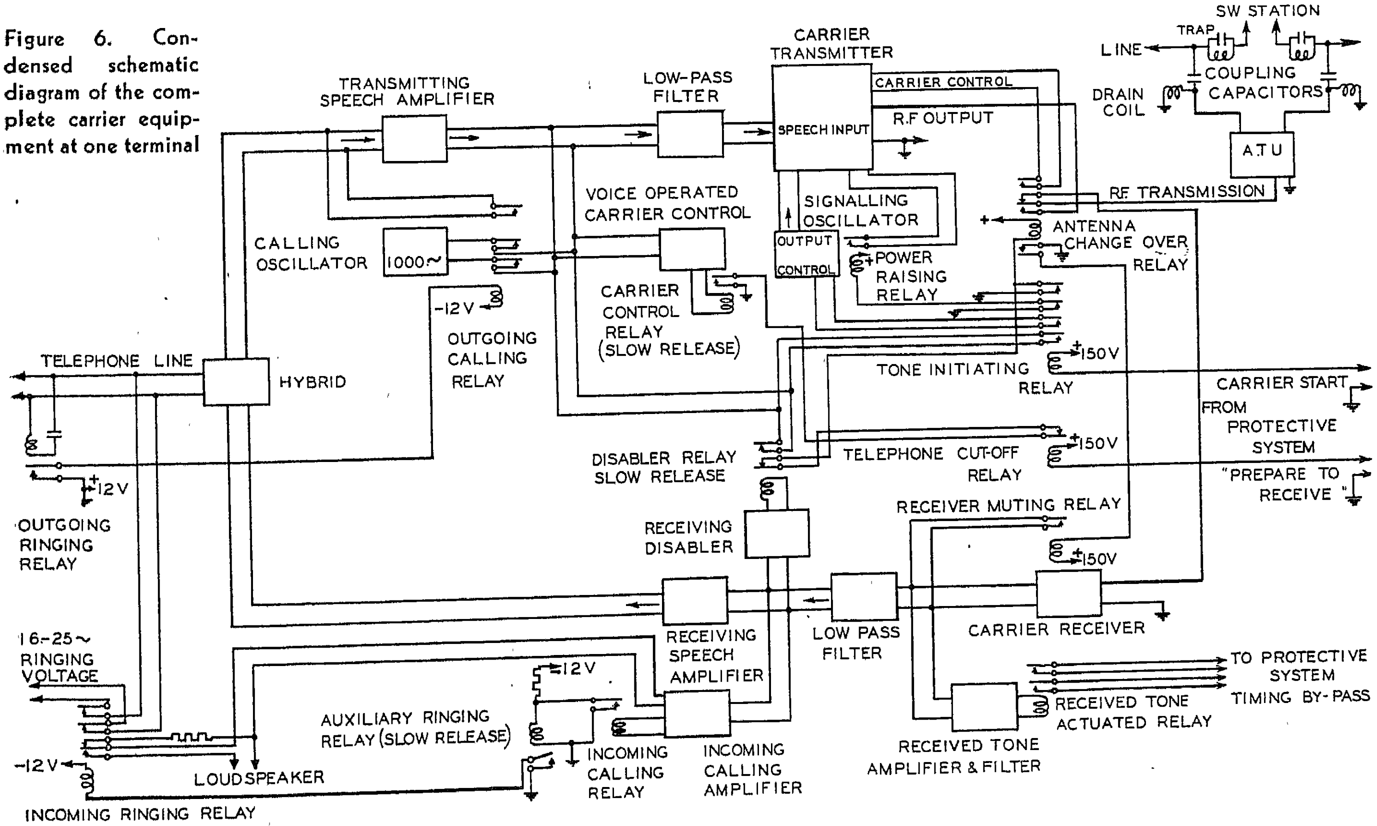

Figure 6. Condensed schematic diagram of the complete carrier equipment at one terminal

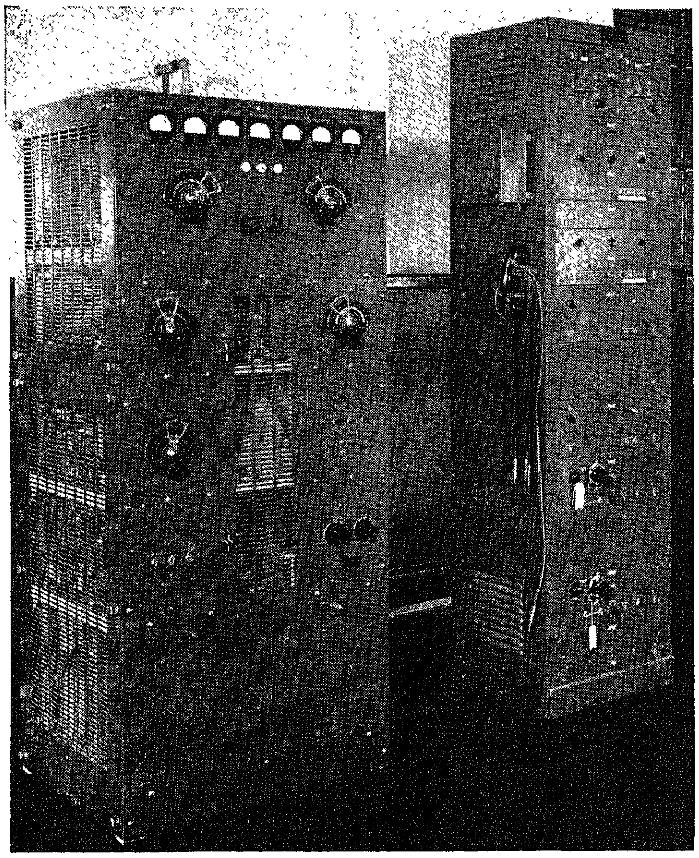

Figure 7. Front view of carrier transmitter and control cabinet

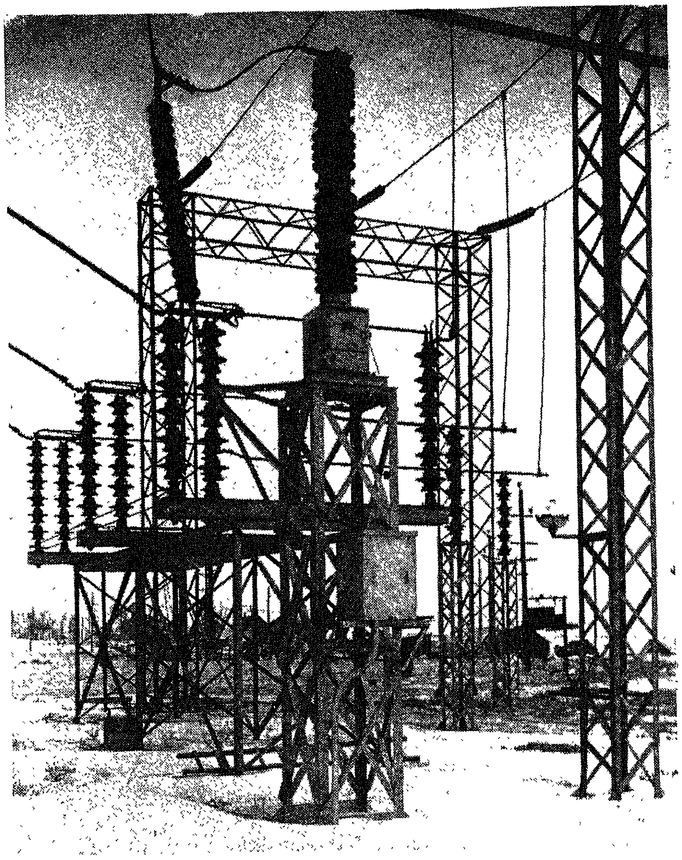

Figure 8. Antenna tuning unit and 220-kv coupling capacitor

for 110 milliwatts ouput. The normal input to the receiver may be as much as one or two volts, depending on transmitter power and attenuation between sending and receiving stations.

The receiving amplifier consists of the following: a single-stage speech amplifier to raise and control the level of the received speech prior to feeding the telephone line, a two-stage amplifier for the calling circuits, and a voice-operated disabling device. The output of the calling amplifier is divided two ways, part operating a conventional loudspeaker, and part being rectified for the operation of the various calling and ringing relays. The disabler includes gas-filled rectifier tubes which are triggered by the incoming speech from the radio frequency receiver, causing the closure of a relay which acts to prevent the operation of the voice-operated carrier control if an attempt should be made to transmit while speech is being received. Moreover, the disabler acts to block the passage of received speech which might pass through the hybrid and enter the transmitter. The relay is a slow-release type and remains closed for approximately half a second after cessation of speech, to avoid operation between syllables or short pauses.

The transmitting amplifier is similar to the receiving amplifier and includes the following: a single-stage speech amplifier to raise and control the level of the speech appearing on the telephone line before it enters the transmitter, a voice-operated carrier-control device and a 1000-cycle calling oscillator with amplifier. The carrier control circuit is essentially the same as the disabler which has been described, except that the relay, which is governed by the speech-triggered gas tubes, is arranged to operate the carrier control and changeover relay in the transmitter. The relay actuated by speech is arranged for a releasing time of approximately one quarter second, so as to hold in between syllables and short pauses in the speech.

One of the low-pass filters is connected in the output from the transmitting amplifier, and the other in the input to the receiving amplifier. The purpose of the former is to avoid modulating the transmitter by components of speech above about 2,500 cycles. If these components lie in the vicinity of 3,000 to 5,000 cycles, they would tend to operate the relaying equipment which is responsive to these frequencies. The purpose of the latter is to exclude the signalling tones from the telephone line at the receiving end.

The tone generators are identical for the three frequencies in use, the only difference being in the constants of the oscillating circuit. The oscillator functions at all times, and the amplifier following it is blocked off at all times except when it is required. This blocking is removed when a tone initiating relay on the transmitter is closed by the operation of the 25-cycle protective system.

The filter amplifiers are identical for the three frequencies in use, the only difference being in the elements of the band-pass filters which are employed. Each consists of a two stage amplifier that is bridged on the output of the radio receiver. The first stage serves to isolate the filter from the line and to allow control of level. The second stage is a power pentode whose output is rectified, and the resultant direct current is used to actuate a contactor. Between the two stages, there is inserted a single section band-pass filter with a band width of approximately 400 cycles. The mid-band frequencies are 3,000, 4,000, and 5,000 cycles. The contactor is energized by the rectified output of the amplifier following the filter, and its contacts operate into the 25-cycle protective system.

The calling unit is a combination of subsidiary apparatus. It includes the hybrid coil and balancing network which couple between the two-wire telephone line and the four-wire communication channel represented by the transmitter input and the receiver output. A small rectifier of the dry-disk type with a filtering system is the supply for the local hand-set. The unit also carries sundry relays associated with the calling system. The low-frequency ringing current on the telephone line is converted into 1,000-cycle current which modulates the carrier. At the receiving end, the 1,000-cycle current operates both the loudspeaker and the relay which impresses the low-frequency ringing current on the called telephone line. The loudspeaker is cut off normally, being in operation only when a call is coming in.

Line Tuning Unit

This unit is mounted out of doors adjacent to the coupling capacitors. It contains the necessary coupling devices to transfer energy from the carrier frequency transmission cable to the 25 cycle high-voltage lines. Also it contains the loading inductances to tune out the reactance of the coupling capacitors. Two separate coupling and loading systems are contained in the unit, so that energy can be fed into two lines in any proportion. As the carrier frequencies are relatively high, the various coils are wound of stranded high-frequency cable. Air core coupling transformers are employed.

Operation

A condensed schematic diagram of the complete system for one terminal is shown in Figure 6.

(*a*) Signalling Tone Transmission

When a fault occurs near one end of the line which is 300 miles long, an impedance relay at the terminal near the fault closes its contact instantaneously. This operation trips the breaker at the near end, and at the same time starts the transmission of carrier modulated at 3,000 cycles by energizing the appropriate auxiliary tone-initiating relay which takes complete control of the carrier equipment at that terminal. This relay actuates the antenna changeover relay, which, in turn, mutes the receiver and starts both the carrier and the 3,000-cycle relaying or signalling oscillator. At the same time, the power-raising relay is actuated to increase the power of the transmitter to its full value. The transmitter then emits carrier, modulated only by the 3,000-cycle tone, for as long as the auxiliary tone-initiating relay is kept energized.

If the transmitter be in use at the time when a tone must be transmitted from the same terminal, the antenna changeover relay will be in the "transmit" position already, because of the action of the voice-operated carrier-control device. In this case, the tone-initiating relay overrides the talker, and the power is raised. The complete relaying operation will be as fast, or a little faster, than usual, because the carrier had been turned on previously. Moreover, the speech line is short-circuited to de-energize the voice-operated carrier control, and to avoid using up any of the modulation capability of the transmitter by the speech which is of secondary importance at the moment. As soon as the tone-initiating relay is de-energized, the system is restored to normal, and the remainder of the talker's conversation will be transmitted as usual, the power dropping back to its original level.

If two or three relaying tones are required, they can be modulated simultaneously at the expense of a small reduction in the modulation depth of each. This reduction is not so great as to impair the operation of the relays at the distant end.

(*b*) Signalling Tone Reception

The tones, demodulated from the carrier, appear at the output of the carrier receiver and enter the received tone amplifiers and filters. The low-pass filter blocks them from the receiving speech amplifier and prevents them from being heard by the listener if a conversation is in progress. The band-pass filters in the amplifiers separate the wanted tone from the others, or from speech, and the proper relay is actuated by the received tone. The contacts of this relay put a by-pass across the contacts of the timer on the second impedance range, making this range almost instantaneous.

If the equipment happens to be in use for transmission of one part of a telephone conversation, and a relaying tone is to be received, dependence is placed on the telephone cut-off relay, which is actuated by the second range impedance relays at the same time as the *JD* timer is started. This operation occurs at the same time as the relaying tone is started at the distant end. The telephone cut-off relay opens the circuit to the antenna changeover relay even though it has been closed by the talker. This relay is forced to go to the "receive" position, and the receiver is unblocked. The equipment is thus prepared to receive the tone at the same time as the carrier modulated by the tone arrives.

If the second range impedance relays energize the telephone cut-off relay at the same time as the tone-initiating relay is energized by the trip relay, then the tone-initiating relay overrides all else and the sequence proceeds correctly.

The time which is lost between energizing the tone-initiating relay at the end of the line nearer to the fault and the closure of the relay by the received tone at the distant end is in the order of 0.9 of a cycle on 25 cycles per second, if the equipment at both ends is in the stand-by condition. If the carrier equipment is in use for telephony, and the transmission is in the same direction as the relaying tone must be sent, the lost time is somewhat less, being about 0.5 of a cycle on 25 cycles per second. If the equipment is in use for telephony and the transmission is in the opposite direction to that which is required for the relaying tone, the total time lost is in the order of 1.5 cycles on 25 cycles per second. This is the slowest case because the system must be prepared to receive the tone as described previously.

A general view of the radio-frequency equipment is shown in Figure 7. At this station, these cabinets are 2,400 feet from the antenna tuning unit and about 2,000 feet from the panels on which are mounted the protective relays. The antenna tuning unit and a coupling capacitor are shown in Figure 8.

Conclusions

This carrier relay scheme which uses a tone as a transfer trip impulse, as described above, has several advantages. Standard protection relays of any type may be used. Only one carrier set is required at each station, coupled to as many lines as need this protection. The number of relaying tones or channels is limited only by selectivity of the band-pass filters. The carrier system operates as a telephone circuit normally, and only as a carrier relaying feature for an interval of one second for every line fault. A failure of the carrier increases the clearing time only for those faults which are in the end sections.

Reference

1. The 220,000-Volt System of the Hydro-Electric Power Commission of Ontario—II, A. H. Frampton, E. M. Wood. AIEE Transactions, volume 60, 1941, pages 1215-21.

Temperature Limits and Measurements for Rating of D-C Machines

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Synopsis: Existing industrial practice for determining the temperature rise of d-c machines is based on measurements with thermometers, placed on exposed or accessible surfaces of the machine. Tests using resistance measurements with improvements in technique have disclosed the existence of considerably higher internal temperatures than those indicated by thermometers, especially for short-time rated machines. Experience has indicated, nevertheless, that such machines have entirely satisfactory service records.

Greater accuracy and consistency are possible with the resistance method, with proper measurement techniques, and it affords a truer indication of winding temperatures. This paper, therefore, suggests that the resistance method should be more generally used and should ultimately be recognized in the Standards as is now the case for railway motors. With this in view, detailed recommendations for resistance-measurement technique are presented.

Correlation of the temperature rises determined by the resistance method with those measured by the thermometer method is necessary if resistance methods are to supplant the now standard thermometer methods. This paper shows the relation existing between temperature rises measured by thermometer and those measured by resistance on variously ventilated and insulated machines.

Values for temperature limits measured by resistance for continuous and short-time rated class-*A* and class-*B* insulated d-c machines are presented. These are consistent with the recently published AIEE Standard 1*A* "Report on General Principles for Rating of Electrical Apparatus for Short-time, Intermittent or Varying Duty." The information presented in this paper is intended to apply primarily to low-voltage (approximately 600 volts maximum) d-c machines with form-wound armature coils and conventional types of field coils.

Temperature Measurements

STANDARDS for rating d-c machines provide temperature limitations for the protection of winding insulation. Although there are three recognized methods of temperature measurement, namely,

1. Thermometer method
2. Resistance method
3. Embedded-detector method

the present AIEE practice is based on the thermometer method.

The embedded-detector method is used to determine interior temperatures at designated locations within windings. It is also used in small motor windings, with thermocouples, in specified locations inaccessible to mercury or alcohol thermometers.

The thermometer method is perhaps the easiest to apply, at least for open-type machines, and requires the least amount of equipment. Although the thermometer method has been used for many years with varying degrees of success, it indicates a temperature that is generally less than the internal temperature of the winding being measured. The thermometer may register a temperature very much less than the average copper temperature, depending on the size of motor, length of heat run, ventilation, internal temperature, and insulation. If within a motor one winding is 20 degrees centigrade hotter than the other winding, the thermometer on the cooler winding may actually register a temperature higher than the average copper temperature of that winding. In practically all cases, however, and especially for short-time ratings, the thermometer on the hottest winding will register a temperature less than the average copper temperature of that winding.

The thermometer method has met the needs of industry for many years, and is still adequate for practical purposes. However, it is difficult to obtain consistent results by the thermometer method, because of variations in surface conditions, thermometer locations, and ventilation. Quite often heat runs on duplicate machines or on the same machine

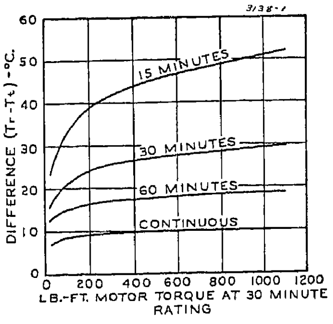

Figure 1. Temperature difference of resistance rise minus thermometer rise versus size of motor for various lengths of heat runs of class-*A*-insulated totally enclosed motors

T_r = Temperature rise by resistance, degrees centigrade

T_t = Temperature rise by thermometer, degrees centigrade

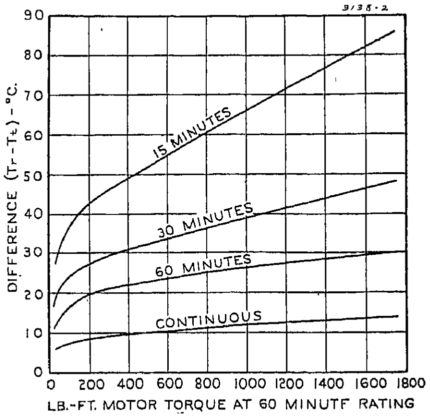

Figure 2. Temperature difference of resistance rise minus thermometer rise versus size of motor for various lengths of heat runs of class-*B*-insulated totally enclosed motors

T_r = Temperature rise by resistance, degrees centigrade

T_t = Temperature rise by thermometer, degrees centigrade

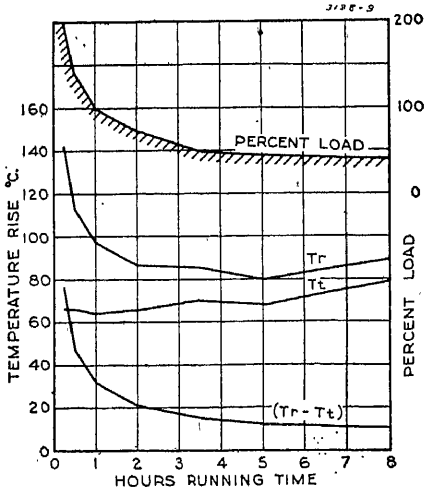

Figure 3. Temperature rise versus length of heat run on a totally enclosed motor

Shaded curve shows per cent load applied for each heat run

T_r = Temperature rise by resistance, degrees centigrade

T_t = Temperature rise by thermometer, degrees centigrade

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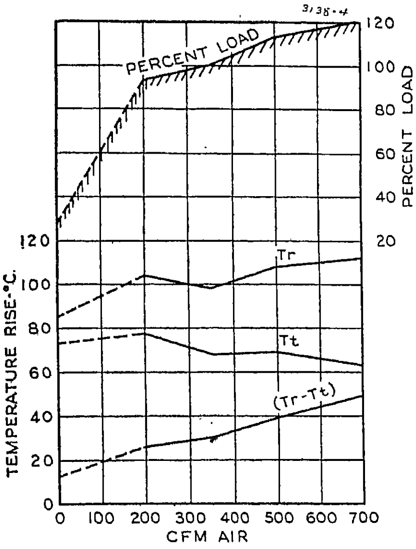

Figure 4. Temperature rise versus volume of ventilating air for continuous heat runs on a blown motor

Shaded curve shows per cent load applied for each heat run. Zero air heat run taken on totally enclosed motor

T_r = Temperature rise by resistance, degrees centigrade

T_t = Temperature rise by thermometer, degrees centigrade

will not check previous thermometer tests with a reasonable degree of accuracy. Another shortcoming of the thermometer method is that it does not indicate the internal temperature of the winding.

The resistance method gives the average temperature of the copper in the winding being measured. This method requires more equipment than the thermometer method, but, when it is applied with the proper technique, more accurate and consistent results may be expected. The main advantage of the resistance method is that it indicates the average copper temperature within the winding, which is generally closer to the hot-spot temperature in any part of the winding than would be indicated by the thermometer method.

The rise by resistance is, in general, higher than the rise by thermometer, and the former should, therefore, be a better indication of the temperature which limits insulation life.

Temperature Ratings

For many years it has been the accepted practice to limit the temperature rise on d-c machines to predetermined values, depending on the class of insulation, ambient temperature, and degree of enclosure. The allowable temperature rises were derived from hot-spot temperatures, which were established as being the maximum that a particular class of insulation could withstand continuously and give satisfactory winding life. AIEE Standards 5 (D-C Rotating Machines, Generators and Motors) and 45 (Electrical Installations on Shipboard) and American Standard C50 (Rotating Electrical Machinery) currently specify that the temperature rise be measured by thermometer, and that the limiting observable rises on insulated windings for a 40-degree-centigrade ambient temperature be as follows:

	Degrees Centigrade	
	Class-A Insulation	Class-B Insulation
Totally enclosed	55	75
Open and semienclosed	50	70

General-purpose machines with a service factor of 1.15 are rated 40 degrees centigrade rise for class-A insulation. Although the foregoing temperature-rise limits were established primarily for continuous-rated machines, they have been applied without change or allowance to short-time-rated motors. The most commonly used short-time ratings are 30 minutes and 60 minutes, but 15-minute ratings are also used. The majority of crane motors carry a 30-minute rating, although they may be used in continuously repeated duty-cycle service. Marine-type motors for deck machinery are rated on a short-time basis of 30 or 60 minutes, generally on the former of these two bases. Since the majority of crane motors, and practically all marine deck motors, are totally enclosed, they have been built with short-time horsepower ratings limited by temperature rises of 55 degrees centigrade for class-A insulation and 75 degrees centigrade for class-B insulation, as determined by the thermometer method of measuring temperature. Many thousands of such motors have been in service for years and have given satisfactory performance. The satisfactory life of the insulation in such short-time-rated motors is due as much to the careful applications of the motors as to the temperature rise on which their rating is based. These motors, although rated on a short-time basis, are generally applied on a duty-cycle basis. In applying such motors, it is common practice to calculate the root-mean-square load, or the average watts loss of the cycle, to make sure that the motor has sufficient thermal capacity, assuming that the duty cycle is to be repeated continuously or for a specified period, so that it can be operated without the allowable temperature rise being exceeded.

Thus it is seen that a short-time horsepower rating is merely a "gauge" which gives the user a conception of the physical size and torque capacity of the motor, but which conveys nothing concerning the continuous thermal capacity of the motor. Although the temperature rise measured by thermometer at the end of the short-time heat run is equal to or less than the allowable rise of 55 degrees centigrade for class-A or 75 degrees centigrade for class-B insulation, the rise indicated by resistance measurements is greater than these values.

In a previous paper[8] data were presented to show the difference between temperature rises of d-c machines when determined by the thermometer method as compared with those determined by the resistance method. These data were taken from tests on class-B-insulated totally enclosed motors. The present paper presents additional data on differences between rise by thermometer and rise by resistance. Information is given on totally enclosed, self-ventilated, and blown motors, with class-A and class-B insulation, and on various sizes of motors.

The amount by which the rise measured by resistance exceeds the rise measured by thermometer for various time ratings for various sizes of totally enclosed motors is shown in Figures 1 and 2. The temperature-rise differential (resistance rise–thermometer rise) is shown plotted as a

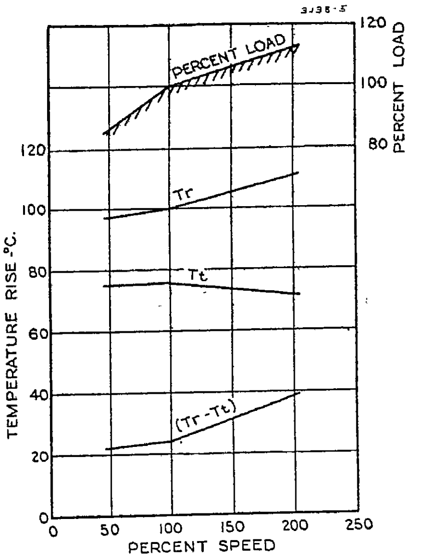

Figure 5. Temperature rise versus speed for continuous heat runs on a self-ventilated motor

Shaded curve shows per cent load applied for each heat run

T_r = Temperature rise by resistance, degrees centigrade

T_t = Temperature rise by thermometer, degrees centigrade function of the 30-minute rated torque for a line of class-*A* motors and as a function of the 60-minute rated torque for a line of class-*B* insulated motors. These curves are plotted in this manner to show the general relation that exists between the various time ratings for a given size of motor, as well as the relation for a given time rating for various sizes of motors, the torque rating being an indication of the physical size of the motor. All heat runs from which the curves of Figure 1 were derived were made to give approximately 55 degrees centigrade rise by thermometer on the hottest winding. Likewise the heat runs for Figure 2 were made to give approximately 75 degrees centigrade rise by thermometer. In practically all cases, the armature was the hottest winding. Hence, the curves of Figures 1 and 2 are all based on armature temperatures. All thermometer measurements were made with mercury thermometers.

From these curves, general relations with respect to temperature differential (resistance rise–thermometer rise) can be expressed as follows:

1. For a given size of motor and thermometer rise, the shorter the time rating the greater the temperature differential. The reason for this is that, the ratio "watt-hours absorbed to watt-hours dissipated" is greater, the shorter the time. The watt-hours absorbed by the windings raise the copper temperature (resistance rise) while those dissipated raise the surface temperature (thermometer rise).

2. For a given time rating and thermometer rise, the larger the machine, the greater the temperature differential. The reason for this is that, the larger the machine, the greater the ratio of watt-hours absorbed to watt-hours dissipated.

3. For a given size of machine and time rating, the higher the thermometer rise, the greater the temperature differential. The reason for this is that, for a given insulation, more watts per square inch are required to produce a higher thermometer rise on the surface of the insulation, and the greater the watts per square inch, the greater the temperature differential.

Figure 3 shows temperature rises both by the thermometer and by resistance obtained from a series of heat runs made on one totally enclosed motor. The shaded curve shows the value of load that was applied for each heat run, expressed as a percentage of the one-hour load.

Figure 4 shows the effect of ventilating air on the differential between resistance rise and thermometer rise for a blown continuous-rated constant-speed motor. Figure 5 shows a similar effect on a self-ventilated motor on which heat runs were taken at various speeds obtained by holding constant field strength and varying the armature voltage. From Figures 4 and 5, it can be seen that, for a given size of motor, the more the ventilating air, the greater will be the difference between the rise by resistance and the rise by thermometer for continuous heat runs with approximately the same thermometer rise.

Figures 1 to 5, inclusive, are based on an average of many tests and are intended to show some of the various factors that cause a variation in the difference between resistance and thermometer rises.

From the foregoing it can be seen that, although it has been standard accepted practice to rate machines 55 degrees centigrade (class *A*) or 75 degrees centigrade (class *B*) rise by thermometer, the rise by resistance exceeds that by thermometer even on totally enclosed continuous heat runs. AIEE Standard 1 now recognizes a temperature differential of ten degrees centigrade between resistance and thermometer measurements for continuous apparatus. This is very close to that obtained from tests on totally enclosed motors, as shown in Figures 1, 2, and 3. However, especially for short-time rated motors, the resistance rise often will exceed the thermometer rise by an amount considerably greater than ten degrees centigrade.

The question that immediately arises is how to reconcile the higher internal temperatures disclosed by this investigation and the satisfactory service experience recorded with the generally recognized temperature-rise limits of 60 degrees centigrade by resistance for class *A* and 80 degrees centigrade for class *B*, given in AIEE Standard 1 and various American Standard and International Electrotechnical Commission Standards. However, the temperature-rise value desirable for any given case depends on many factors besides the insulating materials themselves. The ultimate choice is largely a matter of economics, depending on the relative importance of weight, size, reliability, cost, and life expectancy.

For example, it is well known that 120 degrees centigrade rise is allowed for class-*B* insulated railway motors (with 25 degrees centigrade ambient temperature assumed), whereas only 60 degrees centigrade rise by resistance is normally allowed for the high-voltage armature windings of large alternators with class-*B* insulation (on the basis of 40 degrees centigrade ambient temperature). In the particular case of low-voltage d-c machines, with form-wound armature coils and the usual type of field structure that are alone considered in this paper, the following factors are favorable to the use of higher internal temperatures:

1. The low voltages (approximately 600 volts maximum), the mechanical requirements, and the form-wound construction necessarily result in extremely low dielectric stresses.

2. The low voltage and corresponding small insulation thickness considerably reduce difficulty from varnish-solvent expulsion and coil swelling that limit high-voltage coil temperatures.

3. The relatively short armature-core lengths customary for d-c machines minimize the difficulties from thermal expansion, which are serious in large a-c machines.

4. The type of construction of the d-c rotating armature, including the commutator and the end-binding bands, restricts the exposure of the windings, resulting in a relatively greater difference between internal and external winding temperatures than in the case for induction and synchronous machines. At the same time, the parts of the insulation subjected to the higher temperatures are less exposed to oxidation, moisture, and atmospheric impurities.

Conclusions

In view of the desirability of internal rather than external temperatures as a basis of rating, and the greater accuracy and consistency that may be obtained, it is recommended that improved methods of temperature measurement by resistance be included in the AIEE test code for d-c machines and considered for later adoption as a basis of rating in the Standards. For the latter purpose, the following values of limiting observable temperature rises for both fields and armature windings of d-c machines determined by the resistance method are suggested for consideration:

Clas of Insulation	Limits of Observable Temperature Rises Determined by the Resistance Method for 40 Degrees Centigrade Ambient (Degrees Centigrade)			
	Continuous	One Hour	30 Minutes	15 Minutes
A	65	75	85	95
B	100	110	125	140

1. The values agree with AIEE Standard 1*A*, Table II, short-time ratings, except for the 15-minute rating.

2. The values for class-*B* insulation agree with AIEE Standard 1 Table V, preferred values of standard observable temperature rise for short-time or other special ratings.

3. The values are somewhat more conservative than railway motor values AIEE Standard 11, Table II.

The suggested values are intended to apply to low-voltage (approximately 600 volts maximum,) d-c machines with form-wound armatures and conventional field coils. The values are not intended to apply to other classes of machines where the type of construction and nature of application differ.

Satisfactory experience has been obtained with the suggested values of rise by resistance in machines that meet present thermometer standards. For continuous-rated highly ventilated machines, these values will limit the rise by resistance to a value less than is permissible with the present thermometer standards.

Appendix I. Resistance Measurements

In order to promote the general acceptance of the resistance method of determining temperature rises, and thereby further improve the technique of this method, the following description of a method for determining temperature rise by resistance is offered.

The temperature by resistance method gives the average temperature of the winding being measured. This method consists of comparing the resistance at the end of a heat run (hot resistance) with the resistance at a known temperature (cold resistance). The temperature rise at the end of a heat run is determined by the following formula, which applies to copper windings only:

$$T-t_a=\frac{R_T-r_t}{r_t}(234.5+t)+t-t_u \qquad (1)$$

in which

R_T = resistance in ohms of the winding at temperature T degrees centigrade (this will be the hot resistance measured at an unknown temperature T at the end of a heat run)

r_t = resistance in ohms of the winding at temperature t degrees centigrade (this will be the cold resistance measured at a known temperature t)

t_a = the mean ambient air temperature in degrees centigrade during the last quarter of the duration of the test

$T-t_a$ = the temperature rise in degrees centigrade of the winding above the ambient temperature

A convenient method for determining temperature rise by resistance is as follows:

1. Plot the relation of per cent change in resistance as abscissa against the temperature in degrees centigrade as ordinate. This straight-line relationship is shown in Figure 6 in which a reference temperature of 25 degrees centigrade has been chosen. It is recommended that the scale for the per cent change in resistance be chosen large enough so that it can be read accurately to the first decimal point.

2. Measure the cold resistance of each winding in the motor, and record the temperature of the windings as determined by placing thermometers thereon. Correct the cold resistance from the measured temperature (thermometers on windings) to 25 degrees centigrade. A convenient table for this correction is Table I.

3. Measure the hot resistance at the end of the heat run, and from the percentage increase in resistance $100(R_T-r_{25})/r_{25}$ read from Figure 6 the corresponding temperature. Subtract the ambient or cooling-air temperature t_a to obtain the temperature rise.

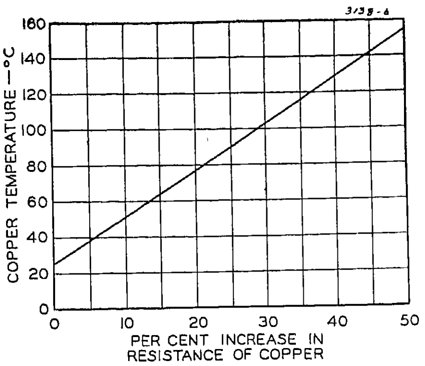

Figure 6. Temperature versus per cent increase in resistance of copper

4. To obtain the temperature rise at the instant of shutdown (at time the power was cut off), record the time at which each hot resistance was taken, the time being measured from the instant of shutdown. Take a series of hot-resistance readings at approximately two-minute intervals for approximately 20 minutes. These points can then be plotted as temperature rise (by resistance) as ordinates and time as abscissae. A curve drawn through these points and extrapolated back to zero time will give the temperature rise at the instant of shutdown. A typical cooling curve is shown in Figure 7.

In some cases, especially at the end of short-time heat runs, the slope of the cooling curve changes rapidly, and there is a large change in temperature during the first few minutes after power is off. It is therefore important to bring the machine to a standstill and take the first reading on each winding as quickly as possible. The cooling curve for the hottest winding generally has the greatest slope, and, since it is the limiting winding in determining the rating of the machine, care should be exercised in determining accurately the zero-time temperature rise on this winding.

There are two methods in common use for determining resistance

(*a*). The drop of potential, or voltmeter–ammeter method. This method is used to best advantage on resistance greater than five ohms.

(*b*). The comparison method in which the unknown resistance is compared with a known resistance by some suitable bridge. For measuring resistances less than five ohms, it is recommended that a Kelvin double bridge be used.

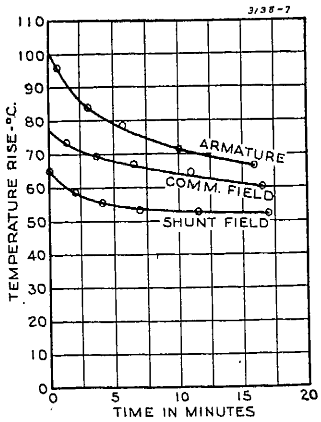

Figure 7. Typical cooling curves determined by the resistance method

Time is measured from instant of power off; taken after a continuous blown heat run

The success of measuring resistance by either the voltmeter–ammeter method or the bridge method depends to a great extent on the wiring and connections between the instruments or bridge and the machine windings being measured. Since for most d-c motors there are at least three elements to be measured—armature, commutating field, and main field—it is important that the wiring of the test table be so arranged that readings on all windings may be made at the end of a heat run in the minimum amount of time and by using only one set of instruments and/or one bridge. To accomplish this in the minimum time and with the least confusion, it is recommended that switches be used in the voltage circuits.

A convenient arrangement of test-table wiring for measuring cold and hot resistances is shown in Figure 8. Such an arrangement can be installed on individual test tables or on a portable resistance table which can be moved to any test stand.

Figure 8 shows the test-table wiring, the motor windings, and a Kelvin double bridge. The bridge is connected to the wiring by an ammeter jack and a voltmeter jack and can, therefore, be replaced readily by an ammeter and a voltmeter. In this diagram are included two extra sets of voltage leads, in addition to the three connected to the field windings which may be used for additional field windings, such as in a motor-generator set. The forks are used for measuring armature resistance. When field resistances are being measured, current is circulated through all field windings. By closing the proper switches, the voltage leads for each winding can be connected to the bridge individually.

Table I

Degrees Centigrade Temperature of Cold Resistance	K	Degrees Centigrade Temperature of Cold Resistance	K
15	1.040	26	0.996
16	1.036	27	0.992
17	1.032	28	0.989
18	1.028	29	0.985
19	1.024	30	0.981
20	1.020	31	0.977
21	1.016	32	0.974
22	1.012	33	0.970
23	1.008	34	0.967
24	1.004	35	0.963
25	1.000		

Multiply cold resistance by K to obtain resistance at 25 degrees centigrade.

The following precautions should be observed in wiring a table; as shown in Figure 8, and in taking resistance measurements to determine temperature rise. Although some of the precautions are not necessary for the voltmeter–ammeter method, they are necessary for the double-bridge method.

1. Each individual voltage lead between the bridge and the field windings should have approximately the same resistance as the other lead in the same pair and should be of relatively low resistance, preferably less than 0.1 ohm per lead.

2. All connections in the voltage leads must be clean and tight. All permanent connections should be soldered.

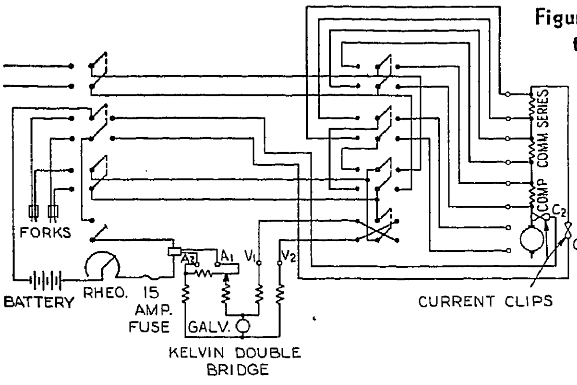

Figure 8. Schematic wiring diagram of test table for measuring resistance

3. All switches must be kept clean and free of dust or corrosion. The swivel connection on all switches between the handle and the base should be shunted with flexible copper shunts, one end of which is soldered to the switch blade and the other end to the connecting wire.

4. Pigtails should be soldered to the field-coil leads, so that the voltage leads can be bolted tightly to the pigtail and not connected at the joint where one field-coil cable connects to another cable.

5. For measuring armature resistance, the forks should be used. In each fork are a current and voltage lead soldered to brass pins ($^1/_8$ inch diameter), insulated from each other and held rigidly in the handle so that pressure can be applied without moving the pins. The forks should be applied to the ends of two commutator segments, securing the maximum span of segments that can be obtained between two adjacent brushholders. The armature should be so positioned that the brushes will not short-circuit armature coils between the span of the forks. The forks should be pressed firmly against the segments so as to break through any oxide film and make good contact with the copper.

6. Cold and hot resistances should be measured under as identical conditions as possible. For the armature, the bars used for determining cold resistance should be marked, and the same pair should be used in measuring hot resistance. In measuring the resistance of field coils, the voltage leads should be connected in such a manner that it will not be necessary to use the reversing switch after cold resistance is taken. The voltage leads should not be removed or changed between the times of taking cold and hot resistance. The current clips should be placed in the same relative position for both cold and hot resistance—this is very important, as will be explained later.

7. In order that errors in reading the double bridge may be reduced to a minimum, the lowest multiplying factor (see Appendix II) with which the bridge can be balanced should be employed. The measurement is independent of the current, but, the higher the value of current within the current capacity of the bridge, the greater will be the sensitivity of the bridge and, therefore, the accuracy of the readings.

8. Cold resistance should not be taken until all windings are within three degrees centigrade of room temperature.

9. As soon as power is cut off at the end of the heat run, the machine should be stopped as quickly as possible and all voltmeters disconnected, making certain that there are no paths in parallel with the windings to be measured.

10. The resistance of the current path between A_1 and the point where the current clip C_1 (Figure 8) connects to the series cable should be relatively low and of the same value for cold and hot resistance. The reason for this is explained as follows:

Refer to Figure 9 which shows the schematic connections of the Kelvin double bridge connected to an unknown resistance r_x.

The connection between A_1 and the point where V_1 lead is connected to r_x will be referred to as the d link. In Figure 9 the resistance of d may be zero or infinity, and as long as

$$(r_1+r_{v2})/r_2=(a+r_{v1})/b$$

then

$$r_x/r=(r_1+r_{v2})/r_2$$

where a, b, r_1, and r_2 are the resistances of the ratio arms of the bridge and r_{v1} and r_{v2} are the voltage lead resistances. For a ratio of 0.1, assume $r_1=100$, $r_2=1{,}000$, $a=100$, and $b=1{,}000$. When r_{v1} and r_{v2} are zero, then r_x (unknown) $=0.1r$ (standard). Assume $r_x=0.001$, and each voltage lead r_{v1} and r_{v2} to have one ohm resistance, then an error of one per cent will be introduced, because the standard resistance will have to be adjusted to $0.001\ (1000/101)=0.0099$ in order to balance the bridge—that is, zero amperes through the galvanometer. Even in this case, as long as the ratio $(r_1+r_{v2})/r_2=(a+r_{v1})/b$ exactly, then the resistance of the d link, theoretically, has no effect.

Practically, it is not possible to maintain an exact ratio of $(r_1+r_{v2})/r_2=(a+r_{v1})/b$. Therefore, any resistance in the d link will introduce an error. The magnitude of this error will depend on the difference in ratios of the bridge arms, including leads, and the value of the d resistance.

When the bridge is balanced:

$$\frac{r_x}{r}=\frac{r_1+r_{v2}}{r_2}+\frac{d}{r}\left(\frac{b}{a+r_{v1}+b+d}\right)\times\left(\frac{r_1+r_{v2}}{r_2}-\frac{a+r_{v1}}{b}\right) \qquad (2)$$

In comparing hot to cold resistance to determine temperature rise, approximately the same per cent error would be obtained in the hot resistance as in the cold resistance, thereby minimizing the error in temperature rise, provided that the resistances of the ratio arms, leads, and connections do not change between times of taking hot and cold resistance, and the d link resistance changes in proportion to the standard resistance r.

Since small differences in resistance occur in opening and closing switches, in contact joints, and with temperature change of wires, and the d link resistance does not change in proportion to the standard resistance, good practice dictates that the d link should have relatively low resistance, and the two voltage leads should have approximately the same resistance.

From Figure 8 it can be seen that when the resistance of the compensating field is measured, the commutating and series fields and connecting wires are in the d link. This same arrangement should be kept for both cold and hot resistance. Therefore, the current clips must be placed in the same relative position for both cold and hot resistance. For all practical purposes, the addition of this amount of resistance as compared to the winding being measured should introduce little or no measurable error in determining temperature rise, provided that the same relative connections are maintained for both hot- and cold-resistance measurements. The table wiring should be arranged so that it is not possible to connect the battery and rheostat into the d link.

The possible magnitude of error in resistance measurement caused by resistances in the d link and in the voltage leads is discussed in Appendix II.

The discussion in Appendix II is offered to point out the possibility of error in resistance measurement, and each person employing the double bridge will have to determine from actual experience and checking what refinements are necessary to give reasonably accurate results. If too many refinements are attempted, then the task of measuring resistance on heat runs becomes tedious, and the expense and trouble of such refinements may prove unwarranted. It must not be assumed mistakenly that a small amount of resistance in one of the voltage leads makes little or no difference, as is the case when using the voltmeter-ammeter method. Where a voltmeter may have a resistance of 1,000 ohms, 0.1 ohm inserted in one of the leads would give approximately 0.01 per cent error. The same value inserted in one of the voltage leads of the Kelvin double bridge may produce a considerable error, depending on the value of the unknown and the d link resistances.

The circuits shown in Figures 8 and 9 have been in use in one testing department for a number of years in determining temperature rise by resistance and have given reliable results. If such circuits are installed properly and operated with care, reliable results may be expected.

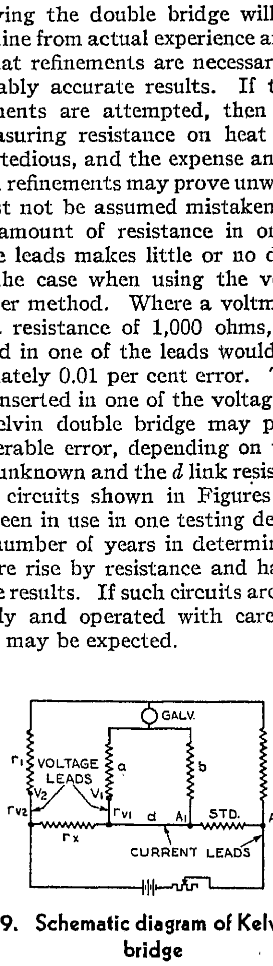

Figure 9. Schematic diagram of Kelvin double bridge

Appendix II. Kelvin Double Bridge

In the double bridge there are a number of adjustments provided in the ratio arms r_1, r_2, a, and b, in order to measure a large range of unknown resistances. For each adjustment $r_1=a$, $r_2=b$, and $r_1/r_2=a/b$. Although in portable forms of the Kelvin bridge $r_1=a$ and $r_2=b$ in most cases, it is not essential that this be so but only that the ratios be equal. The ratio of r_1/r_2 is the multiplying factor which, when applied to the value of observed resistance on the standard, r, gives the value of unknown resistance r_x.

For the purpose of discussion, it is assumed that the double bridge, Figure 9, has three multiplying factors to cover the range of 0.0001 ohm to 1.0 ohm resistance of r_x. It is also assumed that the various resistance arms within the bridge have the following values for each of the three multiplying factors:

Multiplying Factor	Resistance in Ohms		Range of Unknown (r_x) in Ohms
	r_1 and a	r_2 and b	
0.1	100	1,000	0.0001 to 0.01
1.0	500	500	0.001 to 0.1
10.0	1,000	100	0.01 to 1.0

The connecting wires between the bridge and the unknown resistance r_x (Figure 9) will introduce an error in the resistance reading. The magnitude of this error depends on the following factors:

(*a*). Resistance of the voltage leads r_{v1} and r_{v2}.

(*b*). Difference between the resistances of the two voltage leads $(r_{v1}-r_{v2})$.

(*c*). Resistance d introduced in the d link.

Two possible causes of error may be considered.

Case I where $d=0$. From equation 2 $r_x=r(r_1+r_{v2}/r_2)$. In this case an error will be introduced by the resistance of the voltage lead r_{v2} which will change the ratio of r_1/r_2 to $(r_1+r_{v2})/r_2$. Therefore, low-resistance voltage leads should be used, and it is preferable that each lead be less than 0.1 ohm. With a multiplying factor of 0.1 and $r_{v2}=0.1$ ohm, the ratio becomes 100.1/1,000 which gives an error of 0.1 per cent, a value that may be considered negligible. The resistance of the V_1 voltage lead r_{v1} will have no effect on the results so long as d is zero, a condition that may never be realized in actual practice.

Case II where $d>0$ and r_{v1} *or* $r_{v2} \lesseqgtr 0.1$ *ohm*. In this case a change in ratio of the first term on the right-hand side of equation 2 due to r_{v2} can be neglected, because the error will be 0.1 per cent or less, although the second term becomes significant. The error caused by the resistances of the voltage leads and the d link is directly proportional to the d-link resistance and to the difference $(r_{v1}-r_{v2})$ in resistance between the two voltage leads. Therefore, voltage leads of approximately equal resistance should be used as well as a low d-link resistance to minimize the error.

The following general relations apply when there is a difference in the voltage-lead resistance, $r_{v2} \neq r_{v1}$ and $d>0$.

1. For multiplying factors of 0.1 and 10:

Approximate per cent error

$$=0.091(r_{v1}-r_{v2})d/r_x$$

2. For a multiplying factor of 1.0:

Approximate per cent error

$$=0.1(r_{v1}-r_{v2})d/r_x$$

When $r_{v1}>r_{v2}$, the bridge will indicate a value greater than r_x.

When $r_{v1}<r_{v2}$ the bridge will indicate a value less than r_x.

From relations 1 and 2 it can be seen that, the lower the unknown resistance, the greater is the probability of error due to poor wiring and connections. These relations apply only to a limited range of resistance of $(r_{v1}-r_{v2})$ and d. They are, however, approximately correct for values greater than should be permitted in actual practice.

References

1. AMERICAN STANDARD FOR ROTATING ELECTRICAL MACHINERY, C50-1936. Approved January 6, 1936. Pages 23–7.

2. GENERAL PRINCIPLES UPON WHICH TEMPERATURE LIMITS ARE BASED IN THE RATING OF ELECTRICAL MACHINERY AND APPARATUS. AIEE Standard 1, June 1940. Pages 6–9.

3. GENERAL PRINCIPLES FOR RATING OF ELECTRICAL APPARATUS FOR SHORT-TIME, INTERMITTENT, OR VARYING DUTY. AIEE Standard 1A, September 1941. Pages 9–10.

4. RAILWAY MOTORS AND OTHER ROTATING ELECTRICAL MACHINERY ON RAIL CARS AND LOCOMOTIVES. AIEE Standard 11; American Standard C35, 1943.

5. TEST CODE FOR DIRECT-CURRENT MACHINES. AIEE Standard 501, July 1941. Pages 6–7.

6. DETERMINATION OF TEMPERATURE RISE OF INDUCTION MOTORS, **E. R. Summers.** AIEE TRANSACTIONS, volume 58, 1939, September section, pages 459–67.

7. RATING OF GENERAL-PURPOSE INDUCTION MOTORS, **P. L. Alger, T. C. Johnson.** AIEE TRANSACTIONS, volume 58, 1939, September section, pages 445–59.

8. THE APPLICATION OF CLASS-*B* INSULATION TO AUXILIARY-TYPE D-C MOTORS IN SEVERE DUTY SERVICE, **F. A. Compton, Jr.** AIEE TRANSACTIONS, volume 59, 1940, pages 828–34.

9. SOME PROBLEMS IN THE STANDARDIZATION OF TEMPERATURE RATINGS OF FRACTIONAL-HORSEPOWER MOTORS, **Cyril G. Veinott.** AIEE TRANSACTIONS, volume 59, 1940, pages 1055–61, 1218–21.

A New Simple Calculator of Load Flow in A-C Networks

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LOAD-FLOW analysis in a-c networks is increasing in importance because of the greater use of loop circuits in transmission and distribution systems to obtain the advantages of network operation.

To make the best use of materials and man power and to keep system investment at a minimum require careful system planning in advance of working up the details of design. If a simple device is made available to engineers responsible for this system-planning work which will enable them to make more complete analysis of system load flow, voltages, losses, reactive requirements, and so forth, in less time than heretofore possible by analytical solutions, it should aid materially in improving the over-all effectiveness and efficiency of the engineers and the systems they design.

A-c boards, network analyzers, or calculators have been developed to a high degree of perfection and are used extensively by the larger operating companies for major problems involving system interconnections and extensive changes or additions. These boards are useful in many ways, and the only objection to them is that they are relatively large and expensive, and for economic reasons they are not available to the majority of engineers except at times when the larger studies are involved. Every power system has many problems arising from day to day which could be answered better and more completely in less time if there was a simple calculator at hand.

It is the purpose of this paper to set forth certain broad principles which form the basis for the design of entirely new and simpler types of calculators for load-flow analysis of a-c networks. Calculators based on these principles may prove to be the answer to system-planning engineers' requirements for small inexpensive units which will be on hand to solve these problems as they arise. The description of one particular form of calculator is given to illustrate the application of these principles to a specific and practical design. Briefly, this calculator consists of a multitap voltage transformer, a few multitap resistors, a measuring instrument which is similar to a three-phase, three-wire electrodynamometer type of wattmeter, and a number of multicontact multiposition bus switches. In addition to these main elements, there are multiconductor cords with plugs and jacks as required for the interconnection between the units. No generator or load units are required, and the only circuit elements needed are enough multitap resistor assemblies to set up the maximum number of circuits connected to any one bus of the network. For example, if the actual network under study has 100 circuits, and the maximum number of circuits connected to any one bus is six, only six resistor assemblies are required to obtain a complete network setup so that readings can be taken at every bus for generation or load and for load flow in everyone of the 100 circuits.

In references 1 and 2 a new method of a-c network analysis using resistance networks is described. It was shown how the method could be applied to the conventional d-c board by making four separate resistance-network setups to simulate the a-c impedance network. It was further indicated that, by use of a new type of resistance board in which the four resistance networks can be set up simultaneously and handled the same as a single network, it is possible to read directly real and reactive power, current and voltages at all points in the network by means of special metering equipment. This type of board referred to in these previous papers may be classified as a constant-current type, because the generators and loads are setup in terms of current components.

As an outgrowth of the study to obtain direct readings of real and reactive power for the constant-current type of board, the principles fundamental to the simple calculator described in this paper were developed. It may be classified as a constant-potential type, because voltage components are used to set up the generation and loads on the system. The advantage of the constant-potential over the constant-current type is that it requires much less equipment.

On existing a-c boards the load unit settings for real and reactive power are obtained by first assuming what the voltage at each load bus will be under the desired loading condition on the network under study and then computing the resistance and reactance combination which will give the approximate load at each bus. After generation has been set up on the board in approximately correct amounts, it is possible to readjust the loads and generators until the board is brought into correct adjustment for recording the readings. The generators are provided with phase-angle and voltage adjustment, so that the desired amount of real and reactive power can be established by a series of adjustments between generators and between generators and loads. Therefore, before good speed can be attained on setting up an a-c board for load-flow studies, the operator must first learn by experience the technique of bringing the board into correct adjustment for a setup of a particular network and loading condition. After the board is adjusted correctly for one set of readings, it is usually a simple matter to readjust it for other loading conditions or network changes, if the changes in loadings and circuits are not too great.

On the new calculator, described in this paper, the constant-potential source may be either alternating or direct current as desired, but the a-c type is somewhat simpler, and therefore the description is limited to this type. Since the circuit elements are made up of resistors only, it is evident that the flow established through these elements is real power and is of no importance except that it does determine the watt rating of the resistors used in the design of the calculator. The manner in which resistance circuit elements are set up with voltage components impressed across them to obtain a simulated flow of real or reactive power through them is the basic principle of the new calculator. Although one means of measuring this simulated real and reactive power flow is described, there are other methods of accomplishing these measurements.

The simulated real and reactive power flow on the new calculator is obtained by setting up the inphase and quadrature components of voltage, which in the operator's judgment would be likely to exist at each bus in the network under study with a particular loading condition. In effect, this is similar to the technique used in setting up the generator units on an a-c board of the conventional design.

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Because the loads, as well as the generators, are set up in this way, it is evident that more adjustments will be required to bring this new calculator into correct balance for a particular loading condition on the network. Therefore, as with existing a-c boards, the speed with which results can be obtained with this new device depends on the technique of the operator in making a series of adjustments at generator and load points to bring the board into correct balance for reading. It may be granted that the new board requires a greater number of adjustments to obtain a correct balance which will consume additional time. However, this does not mean that the device will not make an excellent showing as compared to other means of analyzing load flow in a-c networks. Also, as with existing a-c boards, after a network has been set up and brought into balance for one particular loading condition, if changes are made in the network or in the loading that are not too great, it will be a simple matter to readjust this board to suit the new conditions.

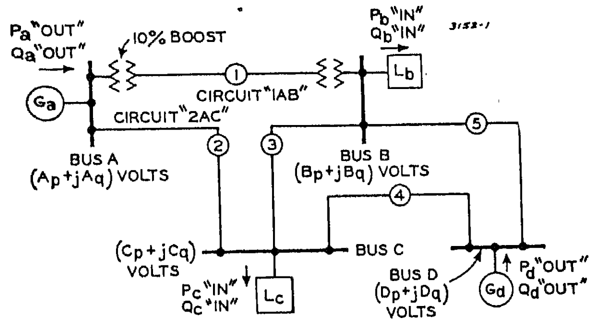

Figure 1 (left). One-line diagram of three-phase network to be used to illustrate connections and operation of calculator

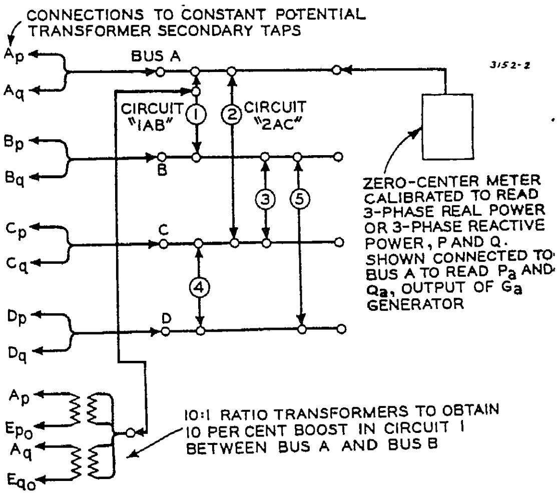
CONNECTIONS TO CONSTANT POTENTIAL TRANSFORMER SECONDARY TAPS
3152-2
Ap
Aq
BUS A
CIRCUIT "IAB"
CIRCUIT "2AC"
1
2
Bp
Bq
B
3
5
Cp
Cq
C
4
Dp
Dq
D
ZERO-CENTER METER CALIBRATED TO READ 3-PHASE REAL POWER OR 3-PHASE REACTIVE POWER, P AND Q. SHOWN CONNECTED TO BUS A TO READ Pa AND Qa, OUTPUT OF Ga GENERATOR
Ap
Epo
Aq
Eqo
10:1 RATIO TRANSFORMERS TO OBTAIN 10 PER CENT BOOST IN CIRCUIT 1 BETWEEN BUS A AND BUS B

Figure 2 (right). Plugging diagram for network of Figure 1, as it would be connected on the calculator

There are some points in the favor of the new calculator in addition to its extreme simplicity in design, small size, and low cost. One point is that it could be handled easily by a single operator. A second point is that, because of the small number of resistances required for circuit elements, they can be of a wattage rating that will allow substantial amounts of current to flow, so that the sensitivity of the measuring instrument need not be so great. A third point is that, because of its simplicity, the network connections can be made in a minimum time. A fourth point is that, by the elimination of generator and load units, it does not necessitate the reduction of an extensive network in advance to bring it down to a certain number of generators or loads before it can be set up on the board.

The circuit theory which is fundamental to this new calculator was presented in detail in the two references and therefore only a brief summary of this theory will be given as a background for the development of the equations for real and reactive power which are basic to the design of the new constant-potential type of calculator.

Mathematical Basis for New Calculator

If a vector current $\mathbf{I}=I_p+jI_q$ is flowing through a three-phase circuit of impedance $\mathbf{Z}=R+jX$ ohms to neutral, the voltage drop to neutral is $\mathbf{V}=V_p+jV_q=(I_pR-I_qX)+j(I_qR+I_pX)$ where vector $\mathbf{V}$ refers to the same reference axis as vector $\mathbf{I}$.

If a vector voltage drop $\mathbf{V}=V_p+jV_q$ occurs in a circuit with impedance $\mathbf{Z}=R+jX$, then the vector current flowing will be $\mathbf{I}=I_p+jI_q=(GV_p+BV_q)+j(GV_q-BV_p)$ where conductance $G=R/Z^2$ and susceptance $B=X/Z^2$.

If the voltage to neutral at one end of the circuit is $\mathbf{E}=E_p+jE_q$ volts, then, with balanced loading on the three phases, at this end the real power is $P=3(E_pI_p+E_qI_q)$, and the reactive power is $Q=3(E_pI_q-E_qI_p)$.

By substitution these equations P and Q may be rewritten

$$P=3E_p(GV_p+BV_q)+3E_q(GV_q-BV_p)$$

and

$$Q=3E_p(GV_q-BV_p)-3E_q(GV_p+BV_q)$$

To put the equations in a more useful form for the calculator design, let $M=1/G$ and $N=1/B$.

$$P=3E_p(V_p/M+V_q/N)+3E_q(V_q/M-V_p/N)$$

and

$$Q=3E_p(V_q/M-V_p/N)-3E_q(V_p/M+V_q/N)$$

In case it is desired to obtain the total net real and reactive power flow in a group of circuits connected to a common bus, the equations may be rewritten as follows:

$$P=3E_p[\Sigma(V_p/M)+\Sigma(V_q/N)]+3E_q[\Sigma(V_q/M-\Sigma(V_p/N)] \quad (1)$$

$$Q=3E_p[\Sigma(V_q/M)-\Sigma(V_p/N)]-3E_q[\Sigma(V_p/M)+\Sigma(V_q/N)] \quad (2)$$

Equation 1 states mathematically that the total net real power in a group of circuits connected to a common bus is equal to the algebraic sum of the real power flow in the individual circuits which combine to make up the group. Equation 2 is similar, except that it covers the same case for reactive power. These two equations are fundamental to the design of the new calculator, and this will be shown by illustrative diagrams later in the paper.

If a generator supplies a group of circuits from a bus, the algebraic sum of the real power flowing in the circuits is a measure of the real power supplied from the generator. Likewise the algebraic sum of the reactive power in the circuits is a measure of the reactive power supplied from the generator.

If a load is supplied from a bus which in turn is supplied from a group of circuits, then the algebraic sum of the real power flowing in the circuits is a measure of the real power supplied to the load. Likewise, the algebraic sum of the reactive power in the circuits is a measure of the reactive power supplied to the load, or, in case the load has leading power factor, it will be a measure of the reactive power supplied from the load.

The convention used in this paper is that power flow in a circuit, or group of circuits, is "out" if it flows away from the bus. As indicated on a zero-center wattmeter, "out" is to the right of zero. If power flow is toward the bus, it is "in" and would be indicated to the left of zero center on the instrument scale.

Reactive power flow in an indicated direction usually carries a negative sign to make it conform to the mathematical relationship it has with the quadrature component of current. In this case, however, the negative sign may be disregarded by using the convention of "out" and "in" which corresponds to the convention previously described for real power.

Calculator Operating Technique

The best way to establish an operating technique for this new calculator is by experience with the device, but it is possible to point out several factors which will aid in determining what procedure might be used in setting up the voltage components for a test on a network.

For the (X/R) ratios usually encountered in the circuits of an electric-power system, the following generalizations may be made:

1. At generator busses, both E_p and E_q would be set at a higher positive voltage tap than at load busses, because real and reactive power flowing in the same direction in a circuit will cause a drop in E_p and E_q. The amount of the drop in the two components will depend on the amount of real and reactive power flowing and the values of X and R for the circuit.
2. At load busses close to generation, both the E_p and E_q would be set at a higher positive voltage tap than at load busses more remote electrically from generation.
3. At load busses where reactive power is supplied to the network because of a synchronous condenser or other capacitance, then the E_p component may be more positive than that at surrounding busses, even though the real power flow is toward this bus. The value of E_q will be less positive or more negative than that of the surrounding busses in this case.

Description of New Calculator as Illustrated by Figures 1 to 4, Inclusive

Figure 1 shows the one-line diagram of a simple three-phase a-c network which is used as a basis for illustrating the setup and operation of the new calculator for determining load flow. Although the network is small and is self-explanatory on the diagram, it should be noted that it covers the pertinent points of generation, loads, and one circuit with step-up and step-down transformers set at different taps so that a net boost is effective in the closed loop of which this circuit is a part.

In setting up the network of Figure 1 on the calculator, it is necessary to establish the circuit constants $M=Z^2/R$ and $N=Z^2/X$ on the common kilovolt-ampere or voltage base of the board. In the case of circuit $1AB$ which includes the two transformers, the lumped-impedance method is used as the basis for obtaining the values for M and N for circuit $1AB$, and then a ten per cent series boost is inserted in this lumped circuit from A to B.

Figure 2 shows the schematic plugging diagram for the network of Figure 1 as it would be set up on the calculator. The movable cords, or other means of adjusting connections, are indicated by the heavy lines with the plugs and jacks shown as arrows and small circles, respectively. It should be noted that the plugging arrangement is simple and straightforward so that the connections can be made quickly.

Figure 3. Schematic diagram of connections when measuring power flow at bus A of the network of Figure 1

Fundamental equation for three-phase power:
$P=3(E_pI_p+E_qI_q)$ where E_p and E_q are to neutral

Measurement of three-phase power fed to bus A by generator G_a is obtained by measuring the algebraic sum of the power flowing from bus A to bus B in circuit 1 and to bus C in circuit 2

$$P_a=3(E_p)_a\left[\frac{(V_p)_{ab}}{M_{ab}}+\frac{(V_p)_{ac}}{M_{ac}}+\frac{(V_q)_{ab}}{N_{ab}}+\frac{(V_q)_{ac}}{N_{ac}}\right]+3(E_q)_a\left[\frac{(V_q)_{ab}}{M_{ab}}+\frac{(V_q)_{ac}}{M_{ac}}-\frac{(V_p)_{ab}}{N_{ab}}-\frac{(V_p)_{ac}}{N_{ac}}\right]$$

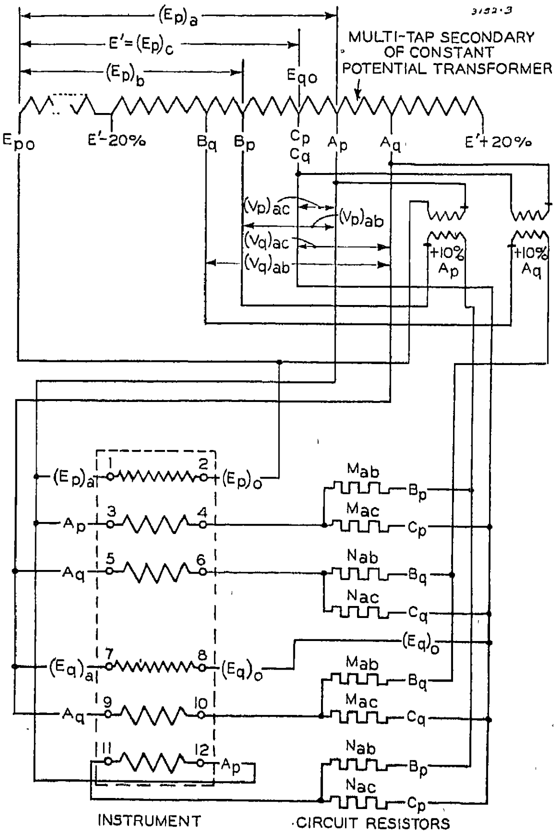

Figure 3 shows the complete wiring for the fundamental circuit for measuring P, real power at bus A of the network. This diagram illustrates how the equation for power in terms of voltage components E_p, V_p, E_q, V_q, and circuit constants M and N may be set up with a measuring instrument to obtain a reading of real power flow. The instrument shown is essentially the same as a three-phase three-wire electrodynamometer type of wattmeter with a zero-center scale. The potential coils connected 1–2 and 7–8 are the moving coils whose combined torque act together to move the pointer. The current coils 3–4 and 5–6 are fixed coils and work together with the potential coil 1–2 to produce the torque of the upper element of the instrument. Likewise the current coils 9–10 and 11–12 are fixed coils which work together with potential coil 7–8 to produce the torque of the lower element of the instrument. The only unusual feature insofar as the instrument is concerned is that there are two fixed current coils on each of the two elements.

Referring to Figure 3, it will be seen that $(E_p)_a$, the inphase component of voltage to neutral at bus A, is acting across potential coil 1–2; that the current in coil 3–4 is the algebraic sum of $(V_p)_{ab}/M_{ab}$ and $(V_p)_{ac}/M_{ac}$; further that the current in coil 5–6 is $(V_q)_{ab}/N_{ab}+(V_q)_{ac}/N_{ac}$. The torque developed in the top element of the instrument is proportional to

$$(E_p)_a[(V_p)_{ab}/M_{ab}+(V_p)_{ac}/M_{ac}+(V_q)_{ab}/N_{ab}+(V_q)_{ac}/N_{ac}]$$

If the potentials and currents are traced for the lower element of the instrument, it will be seen that the torque developed is proportional to

$$(E_q)_a[(V_q)_{ab}/M_{ab}+(V_q)_{ac}/M_{ac}-(V_p)_{ab}/N_{ab}-(V_p)_{ac}/N_{ac}]$$

The sum of the upper- and lower-element torques is proportional to $P/3$ in the equation for power, P. The instrument

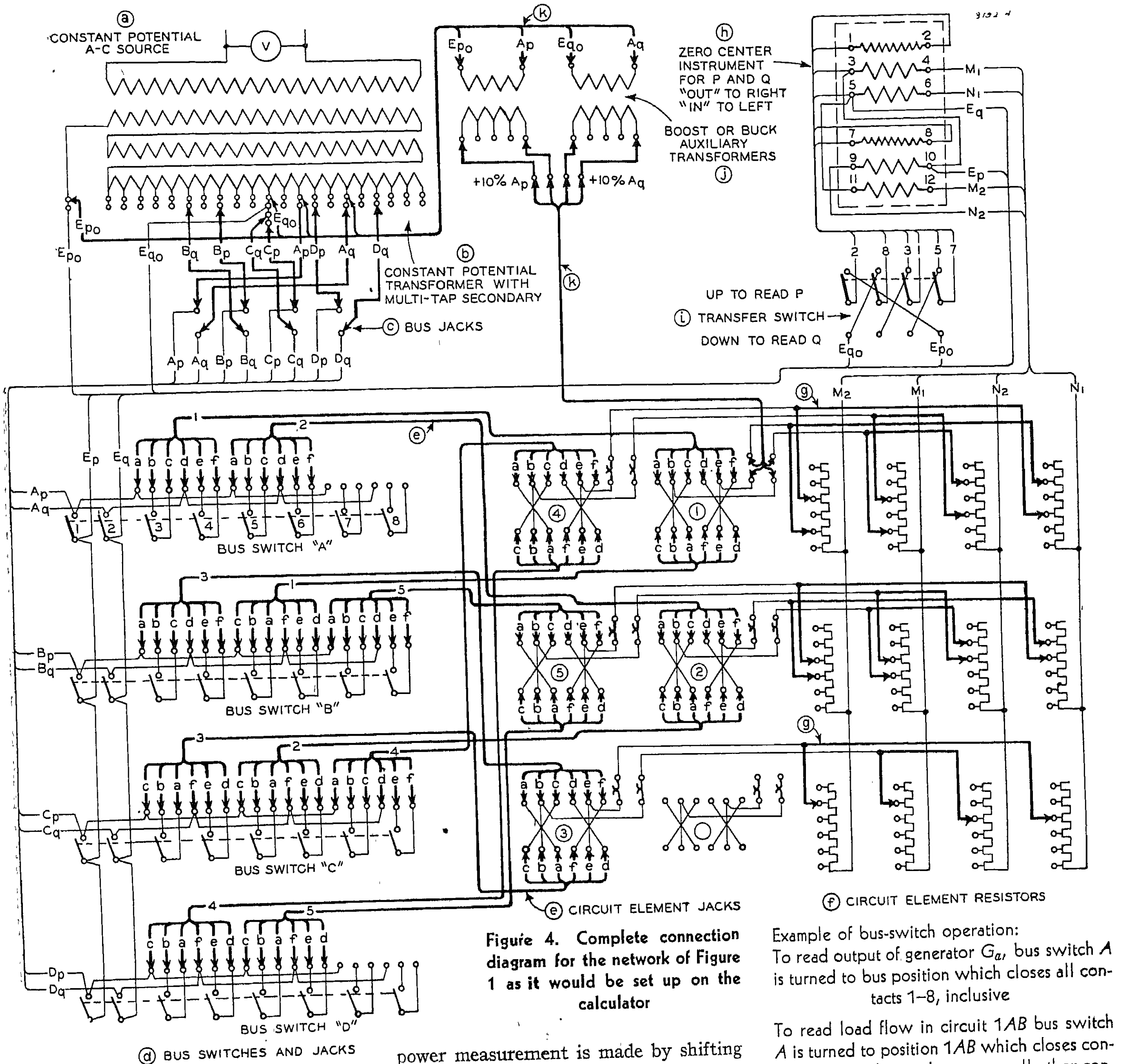

Figure 4. Complete connection diagram for the network of Figure 1 as it would be set up on the calculator

Example of bus-switch operation:
To read output of generator G_a, bus switch A is turned to bus position which closes all contacts 1–8, inclusive

To read load flow in circuit 1AB bus switch A is turned to position 1AB which closes contacts 1–4, inclusive, but opens all other contacts

To read 2AC bus switch A is turned to 2AC which closes contacts 1, 2, 5, 6 but opens all other contacts

Circled letters refer to items described in paper

— Flexible connection cord

O← Plug inserted in jack or one point of a polarized plug on a multiconductor cord

— Lightweight lines indicate fixed wiring

scale calibration may be made to read the three-phase power, P.

This reading for power is the sum of that flowing in the two circuits 1AB and 2AC, and therefore it is also equal to the generator G_a output.

To obtain the power flow in the circuit 1AB it is only necessary to turn the bus A switch to position 1AB which disconnects 1AC circuit from the meter. Likewise, to read power flow in 1AC, the bus A switch would be turned to 1AC position which disconnects 1AB circuit from the meter. This bus switch is not shown on Figure 3, but it is shown on the more complete diagram on Figure 4.

Because of the similarity of the reactive-power measurement to that described for real power, a separate diagram and discussion are not included. The reactive-power measurement is made by shifting E_p and E_q potential connections with respect to the currents by means of a transfer switch as shown on Figure 4.

Figure 4 is a complete wiring diagram of one arrangement for the new constant-potential resistance-circuit type of network calculator. It meets the requirements of the simple plugging arrangement of Figure 2 for the complete setup of the networks shown on Figure 1.

The calculator consists of the following elements:

(*a*). Constant-potential a-c source.

(*b*). Constant-potential transformer with multitap secondary winding.

(*c*). Plugging cords for connecting between transformers secondary tap jacks and bus jacks.

(*d*). Bus switches, multipole and multiposition.

(*e*). Plugging cords for connecting between bus-switch jacks and circuit-element jacks.

(*f*). Circuit-element resistors, multitap.

(*g*). Plugging cords between circuit-element jacks and circuit-element resistors, or the equivalent in multiposition switches.

(*h*). Zero-center measuring instrument for real and reactive power.

(*i*). Transfer switch to connect meter for real-power or reactive-power measurement.

(*j*). Boost or buck auxiliary transformers.

(*k*). Plugging cords for boost or buck transformers.

(*l*). Miscellaneous fixed wiring.

Figure 2 showed the fundamental plugging arrangement in a simple manner, and Figure 3 illustrated the basic principle of the calculator design, so that only a brief discussion is presented on some of the elements as shown on Figure 4.

(*a*). The constant-potential a-c source to the primary winding of the supply transformer should be regulated closely, and there are several static types of constant-voltage regulators on the market which would be ideal for this duty.

(*b*). The constant-potential a-c transformer with tapped secondary winding should be of ample design so that the regulation could be neglected and, by supplying a constant potential to the primary winding, the voltage at the secondary taps would remain at fixed values. The range for the taps as shown would allow for a 40 per cent difference in each of the voltage components over the network, from reference point E^1 or $(E_q)_0$ to plus 20 per cent of E^1, and from reference point to minus 20 per cent of E^1, where E^1 is the value of the reference voltage to neutral. From test computations the secondary taps may be at intervals of approximately 0.5 per cent of E^1. For simplicity on the diagram, taps are shown at intervals of two per cent of E^1.

(*c*). Plugging cords between transformer secondary taps and bus jacks could be replaced with multiposition switches if desired.

(*d*). The bus switches are shown with enough contacts to take care of three circuits per bus. These could be extended to take care of any number of circuits per bus. Probably a logical design would be to use about a six-position switch which would take care of metering the total at the bus and five individual outgoing circuits, and then, when more circuits than five are connected to a single bus, additional bus switches would be assigned to that particular bus to take care of the additional circuits. It is possible to replace the bus switches with bus jacks and cords, but the switches should give a means of speeding up the taking of readings. Another alternate to the bus switches would be the use of small relays actuated by push buttons.

(*e*). The plugging cords between bus-switch jacks and circuit-element jacks are six-conductor flexible cords, all similar. The change in the rotation of the lettering between the connections to the top and bottom jacks is taken care of by cross connection between the jacks, as shown on the diagrams, so that all of the cord plugs are polarized in the same way.

(*f, g*). The circuit-element resistors are multitapped, and four resistors make up a single circuit element. These resistors can be set up in pairs, because two are for the M setting, and two are for the N setting. Although cords are indicated as the means of choosing the proper resistor tap, the same results may be obtained by having a suitable number of rheostat-type multicontact switches. As discussed earlier in the paper, the number of resistor units required for the calculator is dependent on the maximum number of circuits connected to any one bus. This means that each unit may be set up to simulate any number of circuits, provided they do not connect to a common bus.

(*h*). The operation of the zero-center measuring instrument is discussed fully in connection with Figure 3. There is one feature which has not been included. It should be provided with several scales and a multicontact switch for switching from one scale to another to suit the range required for a particular measurement. Probably the multirange would be obtained by the use of multipliers in the two potential circuits.

(*i*). The transfer switch for the measuring instrument operates to transfer the potentials, so that in the up position the instrument reads real power and in the down position it reads reactive power. Because of the simplicity of the board, it might be advisable to have separate instruments for real and for reactive power, so that, when changes are made in potential connections to bring the board into balance, the effect of the changes could be observed on the two instruments at the same time and thereby the adjustment might be speeded up.

(*j*). The boost or buck auxiliary transformers are provided to take care of the effect of net boost or buck in a circuit which forms one branch of a closed loop in the network. The impedance of these should be low as compared to the resistance values used in the circuit elements, so that the transformer introduces a series boost or buck without changing the circuit constants appreciably.

(*k*) and (*l*) require no explanation other than the diagram.

Conclusions

A network calculator, of an entirely new form, and of unusually small proportions in physical size and in the simplicity of its electrical elements, has been described in this paper. It may be used for analyzing a-c networks for load-flow and voltage conditions and for the determination of system losses and reactive requirements. In the form described, it is not applicable to the study of system fault conditions.

Although not specifically covered in the discussion, it is apparent that long lines may be set up on this type of calculator by using constants based upon the nominal or equivalent π. In such a case, the effect of the leading reactive and leakage can be included by adjusting the measurement of generation or load at the two terminals of the transmission line.

In the general discussion section of the paper the technique of operating this new calculator was compared to that now used for complete a-c boards of conventional design. The new calculator is not intended to be the equivalent of the complete a-c board but instead is designed to fill the gap which now exists between the a-c analyzer or calculator and complex analytical methods.

The writer intends to build this simple calculator as soon as it is possible to do so, and, on the basis of operating experience, to determine the board technique which will give the quickest results. If it proves to be slower than the complete a-c board and yet much faster than other methods of analysis, it is believed it will prove to be a completely satisfactory tool.

References

1. METHOD FOR A-C NETWORK ANALYSIS USING RESISTANCE NETWORKS, **Waldo E. Enns.** AIEE TRANSACTIONS, volume 61, 1942, December section, pages 875–80.

2. APPARATUS FOR A-C NETWORK ANALYSIS, **Waldo E. Enns.** United States patent 2,323,588, July 6, 1943.

The Frequencies of Natural Power Oscillations in Interconnected Generating and Distribution Systems

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1. Introduction

THE distribution of power in the steady state of interconnected electric networks with large numbers of generators and of consumers of power is well known for a given system. The flow of energy in detail depends on the distribution of the loads, on the admission of power to the prime movers, and on the data for all the connecting lines. With every change of the power distribution in any branch of the system, transient phenomena are initiated which influence the entire complex of prime movers and generators, motors and other consumers, and intermediate lines. As is often observed in the larger systems, the changes in the flow of energy lead to oscillations of power and frequency, depending on the electrical and mechanical data of machines and network, and because of the magnitude of the active energy in modern machines, this may influence detrimentally the performance of the entire system.

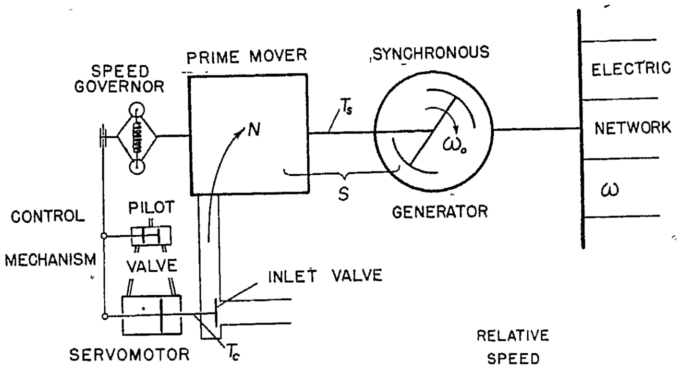
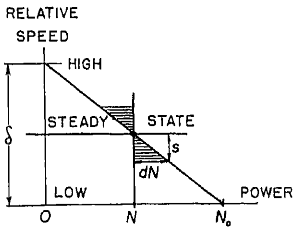


Figure 1. Typical arrangement of generator, prime mover, and speed-governing mechanism

All pure electromagnetic oscillations and similar disturbances, comprising fluctuations merely of the electric and magnetic fields of the conductors, for the most part have frequencies which are higher than the operating frequency of 25 to 60 cycles per second. We will exclude such rapid fluctuations from our consideration since they appear within the electrical part of the system only and have no substantial direct influence on the generation and the use of the a-c energy. Also we will exclude such very slow fluctuations as may be caused by the control of the steam plant proper or the regulation of the flow of water and which have no direct relation to the electrical part of the system. We shall concentrate our attention on those power oscillations which influence the system frequency and change the distribution of the flow of the active energy within and between the various parts of interconnected systems. These electromechanical fluctuations affect the speed of the generators with their prime movers and speed governors as well as the speed of the numerous motors of the consumers.

We are investigating any deviation from the steady-state operation only, and all powers, speeds, frequencies, and position angles may refer, therefore, only to the deviations, leaving open the steady-state values of these magnitudes. For the sake of simplicity, and in order to emphasize the main factors, we neglect unimportant mechanical or electrical losses and any lack of perfection in the working parts of the system.

Figure 2. Droop characteristic of typical speed-governed prime mover



In order to obtain a closed solution of our problem which is fairly complex because of the large number of coupled units most of which have different designs, we introduce further simplifying assumptions which, however, do not affect the general value of the solution. Thus, we will assume that during the transient periods all the main data of machines and network remain constant, that all deviations from the steady state are relatively small and are linear, that unnecessary disturbing effects within the mechanical elements, like backlash or friction in the joints, are avoided, and that the electrical part of the network can be reduced, at least approximately, to a simple form, namely, a radial star system. We do not consider direct-acting speed governors since these play no important part in modern power systems.

2. Power Oscillations of the Single Elements

The entire system here considered may consist of any number of prime movers of different size and type, with like or different speed-governor mechanisms, and driving various synchronous generators, all interconnected to a common system and loaded with any number of induction motors. Our ultimate aim is to determine the amplitudes and frequencies of the deviation of power from the steady-state distribution among the various machines. However, before we consider the influence of the coupling or interconnection of the machines, we will review briefly the well-known analysis of oscillations both of speed-governed prime movers and of synchronous generators and supplement it by a new analysis of transients in induction motors.

A. Prime Mover With Indirect Speed Governor

In Figure 1 a prime mover is shown coupled with a synchronous generator feeding a network and controlled in its speed by a governor with a valve-operating servomotor, as is usual with steam and water turbines. The droop of the speed against generated power is shown in Figure 2.

An excess N of power of the prime mover accelerates the masses of the rotating system, the relative speed deviation of which may be s, so that ds/dt is the relative acceleration. If the electric load of

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the generator remains constant, the energy relation is

$$N+S\frac{ds}{dt}=0 \tag{1}$$

where

$$S=T_sN_0=Ig\omega_0^2 \tag{2}$$

is an inertia constant defined either by the product of the starting time constant T_s and the rated power N_0 of the prime mover, or by twice the kinetic energy of the moving masses having the moment of inertia I, and rotating with the rated mechanical angular speed ω_0, the gravity factor g transforming the units to the practical metric system. The starting time constant of prime mover plus generator is, for the most part, in the limits $T_s=3$ to 20 seconds.

The speed-governing mechanism causes the admission of power to the prime mover to change with time proportional to the speed deviation s so that

$$\frac{dN}{dt}=\frac{N_0}{\delta T_c}s \tag{3}$$

Herein the coefficient of s is taken usually as inversely proportional to the total droop δ of the governor, Figure 2, and to the given total closing time T_c of the valve moving servomotor. For reasons of stabilization, it is customary to affect the movement of the pilot valve by means of a supplementary return, derived from the position of the servomotor itself and proportional to the power admission, so that

$$\frac{dN}{dt}=\frac{N_0}{\delta T_c}s-\frac{N}{T_c} \tag{4}$$

expresses the operation of the speed-governing mechanism for most of the present-day generating systems. The usual limits for the governor droop are $\delta=2$ to 8 per cent, and for the valve closing time are $T_c=0.5$ to 5 seconds.

There are many additional devices for load control or droop compensation, either by hand or automatically, but most of them are intended to affect more the steady-state speed-power characteristic than the deviation during a state of unbalance. Some governor systems have secondary detrimental qualities such as undamped rapid vibrations of their inherent masses, dead bands caused by friction or by play in the joints, nonlinear power characteristic of the inlet valve, and so forth, qualities which even may initiate self-excited oscillations. But these disturbances, if reduced by skillful construction, have only slight influence on the main and unavoidable effects considered in this paper, which are preponderantly due to the quotient $N_0/\delta T_c$, the power velocity of control expressed in kilowatts per second, of the entire governing mechanism.

By substituting for the controlled power of equation 4 the energy of equation 1, we obtain the differential equation for the speed deviation of the prime mover and governor system

$$\frac{d^2s}{dt^2}+\frac{1}{T_c}\frac{ds}{dt}+\frac{N_0}{\delta T_cS}s=0 \tag{5}$$

The solution is given by the free natural oscillation

$$s=F\cdot\epsilon^{-\frac{p_\gamma}{2}t}\cdot\cos\gamma t \tag{6}$$

where the amplitude F expresses the maximum speed or frequency deviation of the generating system from the steady-state value. With use of equation 2,

$$\gamma=\sqrt{\frac{1}{\delta T_cT_s}-\left(\frac{p_\gamma}{2}\right)^2} \tag{7}$$

is the frequency of the speed oscillations, and

$$\frac{p_\gamma}{2}=\frac{1}{2T_c} \tag{8}$$

is the exponent of the decay with time of the amplitude. The oscillation N of the power supplied to the prime mover is given by differentiation of equation 6, substituted in equation 1.

For an average prime-mover unit the droop may be taken as $\delta=5$ per cent, the valve closing time as $T_c=1.5$ seconds and the starting time constant, including the generator, as $T_s=8$ seconds. Then the damping exponent is

$$\frac{p_\gamma}{2}=\frac{1}{2\cdot1.5}=0.33$$

and the frequency

$$\gamma=\sqrt{\frac{100}{5\cdot1.5\cdot8}-0.33^2}=\sqrt{1.67-0.11}$$
$$=1.25\text{, or }0.20\text{ cycles per second}$$

For most of the usual machine systems, the damping effect is moderate and thus influences only slightly the natural frequency γ of equation 7. Hence the significance of the last term in equation 4, responsible for the damping, is negligible and equation 3 can be used in good approximation for all problems concerning merely the frequency of the power oscillations.

B. Electric Generator and Network

Every synchronous generator, as in Figure 3, is coupled elastically with the electric network, because of the magnetic forces between stator and rotor. If the rotor speed varies, electric power P is exchanged with the network, and, if we consider the generator alone, without power variations from the prime mover, the energy relation during acceleration is

$$S\frac{ds}{dt}+P=0 \tag{9}$$

The generator produces synchronous power dependent on the position angle of the pole wheel viewed from the network voltage vector and usually produces, in addition, some asynchronous power by action of damper circuits on the rotor, and dependent on the change with time of this same pole angle. The total electric power of the generator as differing from the steady-state power is, when the angle deviates by θ from the network angle,

$$P=p_s\theta+\frac{p_a}{\omega}\frac{d\theta}{dt} \tag{10}$$

where

$$p_s=kP_0 \tag{11}$$

denotes the synchronizing power of the machine proportional to the rated electric power P_0, and to a synchronizing coefficient k, which in turn depends upon reactances and load of the machine. Furthermore

$$p_a=P_0/s_0 \tag{12}$$

denotes an asynchronous power, with s_0 as slip of the damper attained at rated power, while the damper runs as an induction motor. Finally

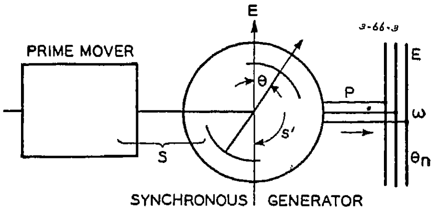

Figure 3. Synchronous generator coupled with prime mover and connected to electric system

$$s'=\frac{1}{\omega}\frac{d\theta}{dt} \tag{13}$$

is the relative slip between the instantaneous electrical speed of the generator and the angular frequency ω of the network, as determined by the machine angle θ.

Numerically, the synchronizing coefficient is for the most part in the limits $k=1$ to 3 and the rated damper slip $s_0=3$ to 30 per cent, while the usual frequency of 60 cycles per second gives $\omega=377$.

The total speed deviation of the pole wheel is given by the sum of the machine and network variations and is expressed by their angles θ and θ_n as

$$s=\frac{1}{\omega}\frac{d(\theta+\theta_n)}{dt} \qquad (14)$$

Introducing equations 10 and 14 in equation 9, we obtain the differential equation

$$\frac{d^2(\theta+\theta_n)}{dt^2}+\frac{p_a}{S}\frac{d\theta}{dt}+\frac{\omega p_s}{S}\theta=0 \qquad (15)$$

which determines the angular movement of the generator for any given variation of the network or system angle θ_n.

If the network is in steady state, the natural oscillations of the pole angle are as solution of equation 15 with $\theta_n=0$.

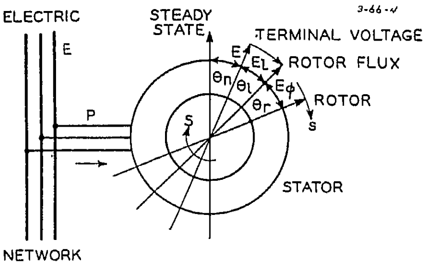

Figure 4. Voltages and phase angles of induction motor fed by fluctuating electric power

$$\theta=A\cdot\epsilon^{-\frac{p_\sigma}{2}t}\cdot\cos\sigma t \qquad (16)$$

Here, with use of equations 2 and 11,

$$\sigma=\sqrt{\frac{\omega p_s}{S}-\left(\frac{p_\sigma}{2}\right)^2}=\sqrt{\frac{\omega k}{T_s}-\left(\frac{p_\sigma}{2}\right)^2} \qquad (17)$$

is the frequency and, with use of equations 2 and 12,

$$\frac{p_\sigma}{2}=\frac{p_a}{2S}=\frac{1}{2s_0T_s} \qquad (18)$$

is the damping exponent of the free oscillations. The power of the oscillations is given by equation 10 substituting equation 16 for the pole angle θ.

For an average generator unit the synchronizing factor may be taken as $k=2$, the rated damper slip as $s_0=10$ per cent, and the starting time constant again as $T_s=8$ seconds. Then the damping exponent is

$$\frac{p_\sigma}{2}=\frac{100}{2\cdot 10\cdot 8}=0.63$$

and the frequency

$$\sigma=\sqrt{\frac{377\cdot 2}{8}-0.63^2}=\sqrt{94.2-0.40}$$
$$=9.67\text{, or 1.54 cycles per second}$$

Damper circuits in actual machines always give a moderate damping factor p_σ. Its influence on the natural frequency σ in equation 17 is thus only slight and we can often neglect the asynchronous power in equation 10 and the second term in the differential equation 15 for the problems concerned only with the frequency of oscillation. It is well known that the synchronizing power and its coefficient k in equation 11 are slightly different for very slow and very fast movements of the pole angle θ. Thus it might be necessary to choose different values for k, depending on the frequency of oscillations resulting from our analysis.

C. Induction Motor and Network

If an asynchronous induction motor, as in Figure 4, is fed by a network with varying phase angle of the voltage E, the deviation θ_n of which is measured against a uniformly rotating or steady-state voltage vector, the motor takes up a varying power, the deviation of which from the steady-state supply we will determine. If the rotor of the induction motor slips against its rotating flux, a power is developed

$$P=P_0\frac{s'}{s_0}=\frac{P_0}{s_0}\frac{1}{\omega}\frac{d\theta_r}{dt} \qquad (19)$$

where P_0 is the rated electric power of the motor, s_0 is the rated asynchronous slip, and s' is the momentary slip determined by the variation of the electrical angle θ_r between rotor and flux.

The effective rotor flux, on the other hand, has a phase displacement with respect to the stator terminal voltage, because of the leakage flux of the motor. This leakage angle θ_l, as shown in Figure 4, is, for small deviation, given directly by the quotient of the momentary leakage voltage E_l and the terminal voltage E so that the angular deviation of terminal voltage and rotor flux is

$$\theta_l=\frac{E_l}{E}=\frac{E_{l0}}{E}\frac{E_l}{E_{l0}}=e_l\frac{P}{P_0} \qquad (20)$$

the leakage voltage being proportional to the current and therefore to the momentary power P. Here $e_l=E_{l0}/E$ denotes the relative leakage voltage of the entire motor at rated load.

For most induction motors the leakage voltage is $e_l=20$ to 30 per cent while the rated slip is in the limits $s_0=0.5$ to 5 to 10 per cent.

According to the law of energy, the power deviation exchanged between network and motor again is determined by

$$P+S\frac{ds}{dt}=0 \qquad (21)$$

where S is here the inertia constant of equation 2 for the motor. The starting time constant of induction motors as quotient S/P_0 is usually in the limits $T_s=0.5$ to 3 seconds.

The total speed deviation s of the rotor, as indicated in Figure 4, is given by the variation with time of the slip angle θ_r, the leakage angle θ_l, and the network angle θ_n in conjunction

$$s=\frac{1}{\omega}\frac{d}{dt}(\theta_r+\theta_l+\theta_n) \qquad (22)$$

Substituting equations 19 and 20 in equation 22, we obtain from equation 21 the differential equation for the electric power deviation

$$P+s_0T_s\frac{dP}{dt}+\frac{e_lT_s}{\omega}\frac{d^2P}{dt^2}=-\frac{T_sP_0}{\omega}\frac{d^2\theta_n}{dt^2} \qquad (23)$$

This result shows that an asynchronous induction motor by variation of the network angle θ_n can be excited to forced oscillations, and that even with a steady-state network it may perform transient natural oscillations which are determined by a zero value of the right-hand side of equation 23.

The power of such free oscillations exchanged between rotor and network is given by the solution

$$P=\overline{P}\cdot\epsilon^{-\frac{p_\mu}{2}t}\cdot\cos\mu t \qquad (24)$$

where P is the amplitude of the oscillation. The natural frequency is

$$\mu=\sqrt{\frac{\omega}{e_lT_s}-\left(\frac{p_\mu}{2}\right)^2} \qquad (25)$$

and the damping exponent is

$$\frac{p_\mu}{2}=\frac{\omega s_0}{2e_l} \qquad (26)$$

It is significant that an induction motor can undergo free oscillations attributable to the effect of the varying leakage voltage, as seen clearly by the third term of the differential equation 23. The natural slip of the rotor, on the other hand, produces a damping effect, as seen by the second term of that equation.

For an average motor with a rated slip $s_0=1$ per cent, a leakage voltage $e_l=25$ per cent, and a starting time constant $T_s=1$ second, the damping exponent is

$$\frac{p_\mu}{2}=\frac{377\cdot 0.01}{2\cdot 0.25}=7.54$$

and the frequency

$$\mu=\sqrt{\frac{377}{0.25\cdot 1}-7.54^2}=\sqrt{1{,}510-57}$$
$$=38.1\text{, or 6.05 cycles per second}$$

This shows that for average values of the motor constants, the natural frequency is relatively high compared with that of synchronous generators, and the damping effect is considerable. It may be noted that the influence of the slip s_0 for induction motors, equation 26, is opposite to that for synchronous generators with damper, equation 18.

Therefore, larger slip of the motor increases the damping of the natural oscillations and with a rated slip

$$s_0 \geqq \sqrt{\frac{4e_l}{\omega T_s}} \tag{27}$$

given by the zero value of the radical in equation 25, the movement becomes aperiodic. Hence a motor with the data just mentioned would lose its natural oscillations if the slip were

$$s_0 \geqq \sqrt{\frac{4 \cdot 0.25}{377 \cdot 1}} = 5.15 \text{ per cent}$$

Small motors are often in this aperiodic range, while larger motors, for the most part, work in the oscillatory range.

At forced fluctuations of the network angle, aperiodic motors follow with a sliding movement, while motors in the periodic range respond according to the laws of resonance. For an oscillation with frequency ν of the network angle θ_n, the forced power exchange of the motor with the network becomes, by equation 23

$$P = \frac{T_s P_0/\omega}{1 + j\nu s_0 T_s - \nu^2 e_l T_s/\omega} \nu^2 \theta_n \tag{28}$$

At frequencies considerably lower than the natural frequency, the denominator of this equation approaches the value one. For example, the frequency $\nu = 9.67$, corresponding to 1.54 cycles per second found previously as inherent for generators, would give for the aforementioned motor a denominator of equation 28

$$1 + j9.67 \frac{1}{100} 1 - 9.67^2 \frac{0.25 \cdot 1}{377}$$
$$= 1 + j0.0967 - 0.062$$

This vector is represented in Figure 5 and shows that for forced oscillations of such frequency the damping effect of the slip is about ten per cent, and the elastic effect of the leakage is only six per cent of the inertia effect of the rotor.

For small damping effect, as present with large induction motors, equation 28 simplifies to

$$P = \frac{-S/\omega}{1 - \left(\frac{\nu}{\mu}\right)^2} \frac{d^2\theta_n}{dt^2} \tag{29}$$

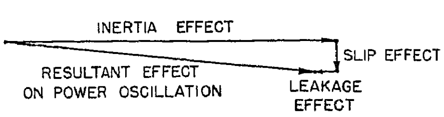

Figure 5. Vector diagram showing the relative effects of inertia, slip, and leakage on slow oscillations of induction motor

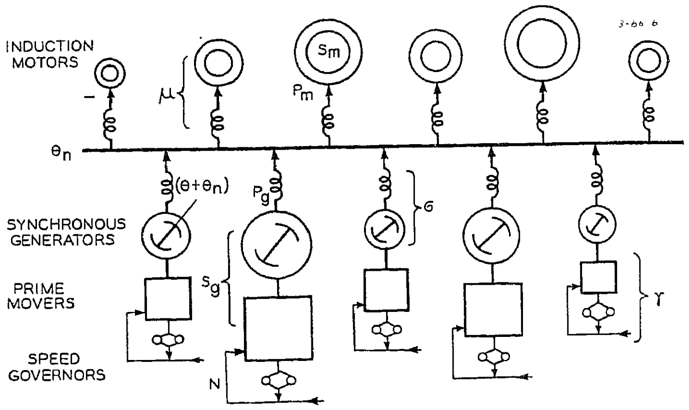

Figure 6. Radial interconnection of many speed-governed synchronous-generator units and numerous induction motors in large systems

showing that the forced power exchange is especially large within the resonance zone of the motor.

Similar to the effect of induction motors is the reaction which commutator motors of various types exert on a network with fluctuating frequency. Their power deviation also is determined by equation 21, if we neglect the secondary effects of load and loss variations. Under certain conditions here also natural frequencies may occur, the principal effect, however, being given again by the action of the inertia of such motors under variable speed conditions.

Almost all the other consumers of the electric power, such as light and heat appliances, electrochemical plants, rectifiers, and so forth, transform the electric energy directly or indirectly into heat or other nonmechanical power. No energy is reversibly stored in them, and thus these devices act only as resistance loads and increase the total damping effects of the system. We shall consider these later and will concentrate our attention at first on those effects which produce oscillations.

3. Interaction of All Prime Movers, Generators, and Motors

The entirety of a large interconnected electrical distribution system for the most part can be transformed either rigorously or approximately into a star system, with many branches connected to one suitably chosen common point. In Figure 6 such a system is shown where each synchronous generator, driven by its prime mover with speed governor, feeds into all the various induction motors through the reactances of the distribution network. These reactances now appear in the radial lines and supplement the internal reactances of the various generators and motors. The resistances of the network may be neglected for the sake of simplicity. We will also omit in this section all the other internal damping effects previously considered, since we found them to play no significant part in the determination of the frequencies of oscillation, their influence being merely of the second order.

In an interconnected system, as in Figure 6, no oscillation of a single prime mover can be performed for itself, since it produces an oscillation of electric power and frequency which spreads through the entire network. Thereby all the other generator units and also all the motors are energized oscillatorily from the unit first considered, and this refers to the machines adjacent in the same power plant as well as to those far distant at the last consumer. Thus all the rotating masses in the system will undergo oscillating forces; however, they will react upon any exciting oscillation by virtue of their own restoring forces, namely, those of their governors, their synchronous and their induction effects. Hence by coupling a multitude of machine units to a complex system all the individually possible oscillations will interfere with one another, resulting in a complicated overall variation of power distribution and network frequency, the details of which we will consider analytically.

A. CHARACTERISTIC FREQUENCY EQUATION FOR ANY NUMBER OF MACHINES

For each generator unit with varying mechanical power input and electric power output, the energy relation is now a combination of equations 1 and 9, namely

$$N + S\frac{ds}{dt} + P = 0 \tag{30}$$

Neglecting the damping effects by using equation 3 for the admission power, and using the first term only of the right-hand side of equation 10 for the electric power, and substituting in all terms the angle θ, by use of equation 14, we obtain

$$\frac{N_0}{\delta T_c} \frac{\theta + \theta_n}{\omega} + \frac{S}{\omega} \frac{d^2(\theta + \theta_n)}{dt^2} + p_s\theta = 0 \tag{31}$$

We can simplify this equation by the

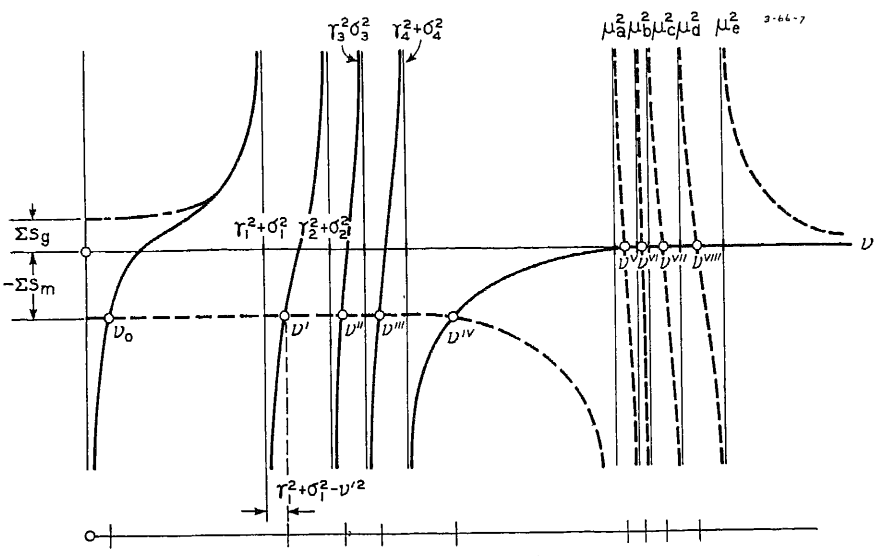

Figure 7. Graphical representation of both sides of characteristic equation for joint natural frequencies of interconnected systems, showing four generator units and five motors

introduction of the inherent natural frequencies γ of the prime mover proper and σ of the synchronous generator proper but now including its line reactance, as in equations 7 and 17, also omitting the damping terms. We have then

$$\frac{d^2(\theta+\theta_n)}{dt^2}+\sigma^2\theta+\gamma^2(\theta+\theta_n)=0 \tag{32}$$

For each of the prime mover plus synchronous generator units we obtain such a differential equation, only with different constants σ and γ. Hence each generator unit will have a different internal pole angle θ, while the angle θ_n for the network system, referring to the star point of Figure 6, is the same for all generators. So we have as many differential equations of the form of equation 32 as there are generator units in operation in the entire interconnected system.

If the system angle θ_n fluctuates, then every motor operates with fluctuating power according to equation 23. We have therefore as many equations of the form 23 as there are individual motors in the entire system, all having the same network angle variation on the right-hand side. Consistently we omit here the second term of equation 23, representing the damping effect attributable to slip variations of the motor, and include in the leakage voltage e_l of the third term the reactive drop of the feeding line.

Since we have excluded losses in the network, the sum of the electric power fluctuations of all the synchronous generators must be negatively equal to the sum of the power fluctuations of all induction motors.
Hence,

$$\Sigma P_g=\Sigma p_s\theta=-\Sigma P_m \tag{33}$$

where the sum of P_g denotes all synchronous power deviations delivered or received by the individual generators according to the first term of equation 10, while the sum of P_m denotes the exchanged power deviations of all the motors according to equation 23.

We can solve these systems of linear differential equations directly by substituting harmonic oscillations for the various angles, namely,

$$\theta_1=A_1\cos\nu t;\ \theta_2=A_2\cos\nu t;\ \theta_3=A_3\cos\nu t;\ \ldots\ \theta_n=A_n\cos\nu t \tag{34}$$

with different amplitudes A_1, A_2, A_3, ... A_n, for generators and network, all having the same frequency ν which is the unknown of our problem. The generator equations of the form of equation 32 then yield the series

$$\left.\begin{aligned}-\nu^2(A_1+A_n)+\sigma_1^2A_1+\gamma_1^2(A_1+A_n)=0\\ -\nu^2(A_2+A_n)+\sigma_2^2A_2+\gamma_2^2(A_2+A_n)=0\\ -\nu^2(A_3+A_n)+\sigma_3^2A_3+\gamma_3^2(A_3+A_n)=0\\ \ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots\end{aligned}\right\} \tag{35}$$

From each equation we obtain the ratio of the specified generator amplitude to the network amplitude of oscillation

$$\frac{A_1}{A_n}=\frac{\nu^2-\gamma_1^2}{\gamma_1^2+\sigma_1^2-\nu^2};\quad \frac{A_2}{A_n}=\frac{\nu^2-\gamma_2^2}{\gamma_2^2+\sigma_2^2-\nu^2};\quad \text{and so forth} \tag{36}$$

On the other hand, equation 33 gives, with use of equation 29 for the motor actions, without damping effect and denoting the various motors by the subscripts a, b, c, and so forth.

$$p_{s1}A_1+p_{s2}A_2+p_{s3}A_3+\ldots$$
$$=-\left[\frac{S_a/\omega}{1-\left(\frac{\nu}{\mu_a}\right)^2}+\frac{S_b/\omega}{1-\left(\frac{\nu}{\mu_b}\right)^2}+\frac{S_c/\omega}{1-\left(\frac{\nu}{\mu_c}\right)^2}+\ldots\right]\nu^2A_n \tag{37}$$

For simplification of this relation we substitute for the synchronizing powers p_s, by use of the middle term of equation 17, the natural generator frequencies σ, omitting the damping term. We then obtain

$$\sigma_1^2S_1A_1+\sigma_2^2S_2A_2+\ldots$$
$$=-\left[\frac{\nu^2S_a}{1-\left(\frac{\nu}{\mu_a}\right)^2}+\frac{\nu^2S_b}{1-\left(\frac{\nu}{\mu_b}\right)^2}+\ldots\right]A_n \tag{38}$$

If we now express the amplitudes A_1 A_2, and so forth, of the pole angles of the individual synchronous machines by their values according to equation 36, the network amplitude A_n cancels, and we obtain finally

$$\left.\begin{aligned}&S_1\frac{\sigma_1^2(\nu^2-\gamma_1^2)}{\nu^2(\gamma_1^2+\sigma_1^2-\nu^2)}+S_2\frac{\sigma_2^2(\nu^2-\gamma_2^2)}{\nu^2(\gamma_2^2+\sigma_2^2-\nu^2)}+S_3\frac{\sigma_3^2(\nu^2-\gamma_3^2)}{\nu^2(\gamma_3^2+\sigma_3^2-\nu^2)}+\ldots\\ &=-S_a\frac{\mu_a^2}{\mu_a^2-\nu^2}-S_b\frac{\mu_b^2}{\mu_b^2-\nu^2}-S_c\frac{\mu_c^2}{\mu_c^2-\nu^2}-\ldots\end{aligned}\right\} \tag{39}$$

From this characteristic equation we can derive the joint natural frequency ν of all the many coupled synchronous and induction machines throughout the interconnected systems, since the inertia constants S and the inherent frequencies γ, σ, μ of the individual machines, the latter including their network reactances, are given.

B. GRAPHICAL SOLUTION FOR THREE TYPES OF OSCILLATION

The solution of equation 39 can be found by graphical representation of both the left-hand and right-hand sides separately, the former belonging only to the generator units, the latter to the motors alone. Figure 7 shows, by solid and dashed lines, the varying value of the two sides of equation 39 dependent on the square of the unknown frequency ν. For $\nu=0$, all the terms on the left-hand side of equation 39 become minus infinity, while all the quotients on the right-hand side become one, so that the sum of all the kinetic energies S_m of the motors appear with negative sign as starting value of the dashed curve in Figure 7.

For increasing ν, let us first consider the simpler right-hand side of equation

39. All the fractions on this side increase at first very slowly, until ν approaches the lowest natural frequency μ_a of one of the motors. The individual term belonging to this motor increases then toward infinity, there changing sign, so that the dashed curve with further increasing ν descends from plus infinity. Since all the other terms on the right-hand side of equation 39 remain negative, the curve soon reaches zero and passes infinity again when ν reaches the second lowest natural frequency μ_b belonging to another motor. This continues as many times as there are motors in the system. Finally, if ν is larger than all the natural motor frequencies, every right-hand-side term has changed to the positive sign and so, with ever-increasing ν, the dashed curve approaches the zero line from above in a hyperbolic manner.

Now we consider the left-hand side of equation 39 by means of Figure 7. With ν increasing from zero, the term with the lowest natural frequency γ_1 first changes its sign when ν passes γ_1, and this occurs successively for all the terms, the γ^2 of which is smaller than the lowest $\gamma_1^2+\sigma_1^2$. Thus the solid curve, with increasing ν, soon becomes positive. It then increases to infinity when ν^2 approaches the lowest $\gamma_1^2+\sigma_1^2$. The curve there changes sign and ascends from minus infinity, reaching plus infinity a second time at the second lowest $\gamma_2^2+\sigma_2^2$. This repeats as many times as there are synchronous generator units feeding the entire interconnected network. Finally the solid generator curve approaches the zero line hyperbolically from below.

As we have seen that numerically the inherent governor frequencies γ are fairly low, that the synchronous-generator frequencies σ are much higher, and that the induction-motor frequencies μ are still higher, we realize that the three regions in which the curves pass through zero or reach infinity are widely separated, as seen in Figure 7. Furthermore, the value of γ^2 is always very small compared with σ^2, for our examples $1.25^2 = 1.57$ in comparison to $9.67^2 = 935$, so that in the denominators of equation 39 γ^2 can often be neglected. If the γ's would vanish completely, as for prime movers without speed governors, the remaining ν^2 in the numerators of the left-hand side of equation 39 would cancel and the terms $S\cdot\sigma^2/(\sigma^2-\nu^2)$ would converge for $\nu=0$ toward the sum of all kinetic energies S_g of the generators, as indicated by the chain-dotted curve end in Figure 7. This shows the similarity between generator and motor effects.

Every point at which the solid curve intersects the dashed curve fulfills the condition of equation 39 and indicates therefore a solution of this equation, giving the value ν_0, ν', ν'', ν''', and so forth, of a natural frequency of oscillation of the entire system. With known constants of all the machines, it is easy to lay out such a frequency diagram. Figure 7 shows that the points of intersection within the generator range are in the order of magnitude of the frequencies σ. However, the actual natural frequencies are somewhat greater than the inherent frequencies of every synchronous machine. The value $\sqrt{\gamma^2+\sigma^2}$ indicates the increase of the electrical frequency by effect of the mechanical power released by the governor which acts in addition to the synchronizing electrical power. With the figures of our examples, we have

$$\sqrt{\gamma^2+\sigma^2}=\sqrt{1.25^2+9.67^2}$$
$$=\sqrt{1.57+935}=9.76$$

indicating an increase of only two per cent. A further increase is due to the effect of the finite value of the motor masses. Only if these approach infinity, would the network become completely rigid and every generator unit would oscillate independently with frequency $\sqrt{\gamma^2+\sigma^2}$, as shown by Figure 7.

If we consider equation 36, we see that for a certain frequency ν the amplitude of that machine only is large compared with the network amplitude A_n, the inherent frequency σ of which is in the neighborhood of the natural frequency ν considered, γ being comparatively small. For all the other machines, the denominators of equation 36 are much larger. We see, therefore, that every synchronous machine oscillates preponderantly with that natural frequency which is the nearest to its inherent frequency but oscillates only slightly with all the other natural frequencies. The actual number of natural frequencies in the generator range equals the number of generators operating in the system.

In the motor range of frequencies, all relations correspond to those in the generator range, except that Figure 7 shows the intersections to be nearer the zero line and hence the differences of the natural and inherent frequencies may be larger for every motor. Therefore the individual motors, according to equations 29 or 37, may not respond with as large amplitudes as the generators do but will oscillate at every natural frequency with moderate magnitude only.

Since the numerical values of the natural frequencies for the generator range are much smaller than those for the motor range, the dashed motor curve in Figure 7 runs through the generator range as a nearly horizontal line of a value $-\Sigma S_m$, the quotients on the right-hand side of equation 39 here all being nearly one. Hence the motors react on the generator oscillations as if their inertias were combined into one large common system inertia, forming the center of inertia of all the network motors.

This is in full accord with equations 28 and 29, showing that the power fluctuation of every motor is thus determined only by its individual inertia S and the variation of the common network angle θ, so long as the actual frequency ν is much smaller than the inherent motor frequency μ. Hence for every slow oscillation, we can combine all the motors into one large equivalent induction motor which has the sum of all the inertia masses of the individual motors. Even if, in some cases, ν/μ should not be small, but of considerable magnitude, the contribution of such motors to the common inertia ought merely to be increased, corresponding to the denominator of equation 29. The larger the sum of the motor inertias, the deeper lies the horizontal part of the dashed line in Figure 7 and the nearer the actual natural frequencies of the generators shift to the inherent values expressed by $\sqrt{\sigma^2+\gamma^2}$.

If there should be a synchronous motor in the system, its inherent frequency σ_x would be moderately high and thus would lie within the synchronous-generator range. However, the value γ^2, expressing the speed-governor action, would vanish in that additional term of the left-hand side of equation 39 which belongs to this motor. Thus that term attains the value

$$S_x\,\frac{\sigma_x^2}{\sigma_x^2-\nu^2}$$

which has the same form as for induction motors except that σ, following equation 17, is much smaller than μ usually is.

For the motor range, on the other hand, with high natural frequencies ν, the solid line of the synchronous generators in Figure 7 approaches the constant value zero, since all the fractions on the left-hand side of equation 39 vanish rapidly with increasing ν^2. Therefore the induction motors oscillate in all their natural frequencies almost as if no synchronous generators were present in the system. The number of intersections and thus the number of natural frequencies in this range, however, is smaller by one than the number of motors in operation.

If there should be an asynchronous

induction generator in the system, driven by a prime mover with speed governor, we must add in equation 39 an expression

$$S_y\ \frac{\mu_y{}^2(\nu^2-\gamma_y{}^2)}{\nu^2(\gamma_y{}^2+\mu_y{}^2-\nu^2)}$$

which is built up like that of the generators but has an inherent frequency μ_y more of the order of that of the motors.

If some or even all of the generators, or some or all of the motors, should have

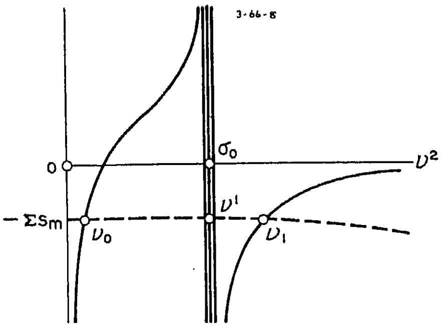

Figure 8. Representation of characteristic equation for four electrically tuned generators

the same inherent frequencies $\sqrt{\sigma^2+\gamma^2}$ or μ, the corresponding frequency quotients in equation 39 become equal, and the corresponding infinities in Figure 7 fall together, forming a multiple intersection so that, according to Figure 8, this natural frequency will coincide with the inherent frequencies of the machines.

C. ELECTRIC TUNING OF THE GENERATORS

If all the synchronous generators are tuned to the same inherent frequency, say σ_0, then Figure 8 shows that besides the multiple natural frequency, $\nu'=\sigma_0$, belonging to the oscillations of all tuned generators against each other, there develops a single higher frequency ν_1 which belongs to an oscillation of all the generators together against all the motors. If we consider all the governor frequencies γ as very small and all the motor frequencies μ as very large as compared with this ν_1, equation 39 becomes in good approximation

$$(S_1+S_2+S_3+\ldots)\frac{\sigma_0{}^2}{\sigma_0{}^2-\nu_1{}^2} = -(S_a+S_b+S_c+\ldots) \quad (40)$$

and thus this singular natural frequency is

$$\nu_1=\sigma_0\sqrt{1+\frac{\Sigma S_g}{\Sigma S_m}} \quad (41)$$

For equal generator- and motor-inertia sums S_g and S_m, this predominant natural frequency would be $\sqrt{2}$ times the inherent generator frequencies. Only for infinite motor inertias, indicating a completely rigid network would this frequency reduce to the inherent frequencies σ_0 proper.

The synchronous power amplitude with which every generator participates in the oscillations is given by equation 10, omitting the second damper term. Using the angle amplitude A of equation 34 and transforming the synchronizing coefficient p_s into the frequency σ by use of the first member only under the radical of the central term of equation 17, and finally using equation 36 with neglected γ's, we obtain successively

$$\left.\begin{aligned} &P_1=p_{s1}A_1=\frac{\sigma_1{}^2S_1}{\omega}A_1=\frac{S_1}{\omega}\ \frac{\sigma_1{}^2\nu^2}{\sigma_1{}^2-\nu^2}A_n \\ &P_2=\frac{S_2}{\omega}\ \frac{\sigma_2{}^2\nu^2}{\sigma_2{}^2-\nu^2}A_n \\ &\cdots\cdots\cdots\cdots\cdots\cdots \end{aligned}\right\} \quad (42)$$

Hence, in the general case, each generator follows its own resonance curve of power with the natural frequency ν varying over the entire spectrum, according to Figure 7, and the power oscillations are large for every generator in the neighborhood of its own inherent frequency σ only.

In case, however, that all the inherent generator frequencies are tuned to the same value σ_0, the frequency quotients in equations 42 become equal for all the generators, since ν_1 is now also a unique frequency given by equation 41. Hence the power oscillations of the individual generator units will be distinguished by their inherent inertia constants S only, the other factors being the same for all. Thus no single generator will oscillate with large amplitude of itself, but the entire natural power oscillation with common frequency ν_1 will occur between all the generators in unison on the one hand, and all the motors in unison on the other hand, every generator participating with a value corresponding to the mechanical size of the machine, as given by its inertia constant S.

Equal inherent frequencies σ, according to equation 17, lead to the condition

$$\frac{k_1}{T_{s1}}=\frac{k_2}{T_{s2}}=\frac{k_3}{T_{s3}}\ldots=\text{constant} \quad (43)$$

for every synchronous machine. This requires the tuning of the synchronizing powers of all the generators, determined by k, to their inertias, determined by the starting time constant T_s. In general, this can be done by proper adjustment of the reactive power distribution among all the generators. Such a system will have the greatest possible stability against too large individual oscillations, or even loss of synchronism by any of the generators, after any disturbance of the equilibrium.

D. SLOW GOVERNOR OSCILLATIONS OF THE ENTIRE SYSTEM

Figure 7 shows that there exists a single very low natural frequency ν_0 given by the first intersection of the solid and dashed curves. We will derive an analytical expression for this frequency, the corresponding power oscillation constituting the slowest and therefore the most significant natural power fluctuation in the system. In the general case the natural motor frequencies and the natural generator frequencies all differ from one another and each being preponderantly correlated to one or a few machines only, these elements oscillate almost at random against one another. The lowest frequency ν_0 however, is coordinated to all the machines, since the frequency quotients of equation 36 with vanishing ν all become of the same order. Therefore, we suspect that they may all oscillate in unison, constituting considerable frequency fluctuations of the entire system.

In a power system under perfect conditions the inherent frequencies σ, determined according to equation 17 by the synchronous forces, are numerically large compared with the frequencies γ, determined according to equation 7 by the governor actions. Figure 7 shows that the σ's are large also compared with the lowest natural frequency ν_0. Finally, the motor frequencies μ, determined by equation 25, also are large compared with the frequency ν_0. We have, therefore, the inequalities

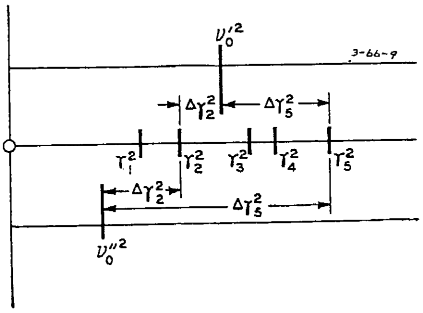

Figure 9. Spectrum of inherent governor frequencies and resultant system oscillations, as significant for distribution of power oscillations among generator units

$$\left.\begin{aligned} &\gamma_1{}^2 \lesseqgtr \sigma_1{}^2,\ \gamma_2{}^2 \lesseqgtr \sigma_2{}^2 \ldots; \\ &\nu_0{}^2 \lesseqgtr \sigma_1{}^2,\ \sigma_2{}^2,\ \sigma_3{}^2 \ldots; \\ &\nu_0{}^2 \lesseqgtr \mu_a{}^2,\ \mu_b{}^2,\ \mu_c{}^2 \ldots \end{aligned}\right\} \quad (44)$$

In the parentheses of the denominators on the left-hand side of equation 39, thus only the values $\sigma_1{}^2$, $\sigma_2{}^2$, $\sigma_3{}^2$ . . . remain, but they cancel against the same magnitudes in the numerators. On the other hand, the quotients on the right-hand side of equation 39 all become one, and thus we obtain in very good approximation

$$S_1 \frac{\nu_0^2-\gamma_1^2}{\nu_0^2}+S_2 \frac{\nu_0^2-\gamma_2^2}{\nu_0^2}+\ldots = -S_a-S_b-S_c \ldots \quad (45)$$

Herein we can collect all the terms with ν_0 on the left-hand side and combine them with those of the right-hand side into a sum containing all generator and motor inertias, S_g and S_m. We then obtain for the lowest natural frequency

$$\nu_0^2=\frac{S_1\gamma_1^2+S_2\gamma_2^2+\ldots}{\Sigma(S_g+S_m)} \quad (46)$$

and if we use for the terms in the numerator equations 2 and 7,

$$\nu_0=\sqrt{\frac{\Sigma N_0/\delta T_c}{\Sigma(S_g+S_m)}} \quad (47)$$

This shows that there exists one slow natural oscillation of the entire system including all the motors, lines, generators, prime movers, and speed governors, the frequency of this oscillation being determined by the sum of the inertias or kinetic energies of all the machines in operation, whether generators or motors or other rotating masses, and by the sum of the quotients $N_0/\delta T_c$ of all the prime movers with their speed-governing mechanisms. We know from equation 3 that this last quotient for every prime mover expresses the power velocity of control, measured in kilowatts per second, with which its speed-governing mechanism tends to restore the steady state. For the various generating units, this quotient may have very different values, depending not only on the rated power N_0 of each unit, but also on the product of the droop δ of the governor and the closing time T_c of the servomotor of the valve. The sum of all these quotients increases the total power delivered to all the prime movers of the entire system and thus every machine plays its individual part in the whole mechanism of oscillation. Any increased power of a prime mover accelerates all the rotating masses of the system, and so all the various machines of the complete system, prime movers with their generators and all the motors, oscillate mechanically slowly in unison with one another, the energy fluctuating between the rotating masses of the entire system and the power sources of the prime movers, be they steam, water, or oil. This is in contrast to the more rapid oscillations of the individual generators or motors, the energy of which fluctuates only between the various rotating masses of the machines.

Hence, with respect to the slow governor frequency, all the rotating masses of the entire system appear as if they were rigidly coupled with one another, and the governor-controlled power admissions of all the prime movers together are acting in unison upon the total inertia. Thus for the slowest and most significant oscillations in interconnected networks we have attained a very simple conception, namely that the entire system acts as a unit with respect to governor powers and inertia masses.

Let us consider, for example, a power system containing only three synchronous prime movers of $N_0=5$, 10, and 20 megawatts rated power, with starting time constants $T_s=8$, 5, and 20 seconds, with speed governors of $\delta=5$, 3, and 6 per cent droop, and servomotors of $T_c=2$, 1, and 4 seconds closing time, while the numerous motors together have an inertia $\Sigma S_m=500$ megawatt-seconds.

The resultant governor frequency of the entire system then is

$$\nu_0=\sqrt{\frac{\frac{5}{5\%\cdot 2}+\frac{10}{3\%\cdot 1}+\frac{20}{6\%\cdot 4}}{8\cdot 5+5\cdot 10+20\cdot 20+500}}$$

$$=\sqrt{\frac{50+333+83 \text{ mw/sec}}{40+50+400+500 \text{ mw-sec}}}$$

$$=\sqrt{\frac{466}{990}}=0.686, \text{ or } 0.109 \text{ cycles per second}$$

while the governor frequencies of the single free-running units would be

$$\gamma_1=\sqrt{\frac{50}{40}}=1.12, \quad \gamma_2=\sqrt{\frac{333}{50}}=2.58,$$

$$\gamma_3=\sqrt{\frac{83}{400}}=0.454$$

This example shows that the medium machine of ten megawatts rated power by its governor of small droop and its servomotor of small valve-closing time produce by far the strongest forces, four times as large as those of the 20-megawatt machine. On the other hand, the inertia is attributed predominantly to the heavy 20-megawatt machine and to the entirety of the motor masses which reduce the frequency considerably. Measurements in power systems show that the starting time constant T_s of the motor inertias related to the generator power in operation, according to equation 2, is of the order of 10 to 20 seconds, this large value being due to the underloading of most of the motors.

If the electric system is severely disturbed, for instance by a short circuit near some of the generators, the synchronizing power of one or more of the electrical machines may decrease. The inherent frequency σ of these machines, given by equation 17, then drops and may come into the neighborhood of the mechanical frequency ν_0 just determined. The total number of natural oscillations is not changed hereby, only that some of the last inequalities 44 do not hold. It is useful in this case to determine all the lowest frequencies graphically, as already shown in Figure 7.

4. Distribution of Power Oscillations

For the faster electromechanical oscillations we have previously seen, by means of equation 36, that the angular amplitudes of the various generators and motors are large for those natural frequencies only which are adjacent to the inherent frequency of the individual machines. The network amplitude A_n of these oscillations will be relatively small, since all machines oscillate against each other.

The speed deviation or the relative frequency oscillation of the network, is given, corresponding to equation 14. quite generally as

$$s_n=\frac{1}{\omega}\frac{d\theta_n}{dt}=-\frac{\nu}{\omega}A_n \sin \nu t=-F \sin \nu t \quad (48)$$

Thus

$$F=\frac{\nu}{\omega}A_n \quad (49)$$

gives the amplitude of the frequency oscillation, constituting a fluctuation of the regular power frequency ω with low frequency ν. This correlation, equation 49 holds for any value of the frequency ν.

The individual power oscillation of every synchronous generator can be determined by equation 10 with its synchronous and also its asynchronous or damper contribution, the amplitude A of the angle θ of each generator being given by equations 36 with respect to the network angle amplitude A_n. Thus the distribution of the power oscillations among the generators can be accurately found for any actual example.

The power oscillation of every induction motor is rigorously given by equation 23 or 28. For moderate damping, however, we can use the much simpler equation 29, and if we confine our consideration to such network frequencies only as are considerably lower than the inherent motor frequencies μ, the second term in the denominator of equation 29 vanishes, and we obtain with use of equation 48,

$$P_m = -\frac{S_m}{\omega}\frac{d^2\theta_n}{dt^2} = -S_m\frac{ds_n}{dt} = \nu F S_m \cos\nu t \quad (50)$$

Thus the motor power amplitudes are given in good approximation by the product of frequency ν and amplitude F of the fluctuation of the network frequency, magnitudes which are the same for all the motors, and of the inertia constant S of the individual motor considered.

The total power fluctuation over the entire system between all generators on the one hand and all motors on the other hand is merely the sum of all individual motor powers of equation 50, namely,

$$\Sigma P_m = \nu F \Sigma S_m \cdot \cos \nu t \quad (51)$$

Its distribution over the various machines of the system and therefore over the various branches of the network is determined by the preceding remarks. This last relation equation 51 holds for the ranges of the synchronous frequencies σ as well as of the governor frequencies γ and ν_0.

For the slow mechanical oscillations of the generators we can use with perfect networks the inequalities 44 for the amplitudes of equation 36 and obtain, with good approximation,

$$\frac{A_1}{A_n} = \frac{\nu_0^2 - \gamma_1^2}{\sigma_1^2}; \quad \frac{A_2}{A_n} = \frac{\nu_0^2 - \gamma_2^2}{\sigma_2^2}; \quad \ldots \quad (52)$$

Since, according to equation 44, both frequencies in the numerators of equation 52 are small compared with the denominators, we see that the amplitudes of the angles $A_1, A_2 \ldots$ between the synchronous machines and the network for these slow governor oscillations are always small compared with the amplitude A_n of the angular oscillation of the network itself. Therefore the total machine oscillations are nearly of the same angular magnitude as the network oscillations A_n, these being of considerable value since all machines swing in unison.

However, we can compare the relatively small angular amplitudes $A_1, A_2, \ldots$ with each other since, according to equations 10 and 34, they are a measure of the power oscillations of the single machines. The synchronous power amplitude of the generators is, with equations 10 and 34, and using the first equation 17, and finally equation 52,

$$\left.\begin{array}{l} P_1 = p_{s1}A_1 = \dfrac{\sigma_1^2 S_1}{\omega} A_1 = \dfrac{S_1}{\omega}(\nu_0^2 - \gamma_1^2)A_n; \\ P_2 = \dfrac{S_2}{\omega}(\nu_0^2 - \gamma_2^2)A_n; \\ \ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots \end{array}\right\} \quad (53)$$

Thus the amplitude of the individual synchronous power oscillation of every machine is determined by its inertia constant S, and the difference of the common governor frequency ν_0 for the entire network and the inherent governor frequency γ of the individual machine. Figure 9 shows as an example the distribution of the inherent governor frequencies γ of five individual prime movers with their synchronous generators, and above and below this frequency spectrum two different values are indicated for the resultant actual governor frequency ν_0 of the entire system. The upper larger value ν_0' may belong to a system with very small motor masses so that, according to equation 47, ν_0' is more in the center of the individual frequencies, while the lower smaller value ν_0'' may belong to a system with larger motor masses which decrease considerably the joint governor frequency. The difference $\Delta\gamma$ between the joint frequency ν_0 and the inherent frequencies γ, in the quadratic scale of the diagram Figure 9 determines, according to the equation 53, the individual synchronous power oscillation of every generator unit. The asynchronous power of the second term in equation 10 usually is small compared with the synchronous power.

Thus all the generator units with different γ participate in very different degrees in the power oscillation of the system. Those with large γ, corresponding to a high velocity of control according to equation 7, deliver large electric power into the network oscillation; those with smaller γ deliver small or even negative power amplitudes into the system. These spectral differences, and therefore the subdivision of the power oscillations among the individual machines, are by no means proportionate to the rated powers of the various machines. For example, a large-sized generating unit may have a large product $\delta T_c T_s$ and therefore, according to equation 7, a small γ, and a small-sized machine of the same system may have a small $\delta T_c T_s$ and therefore a large γ. Then the small machine might participate in these power oscillations to an even greater extent than the large machine, dependent in addition on the inertia constants S. Thus it may be heavily overloaded and may even lose synchronism.

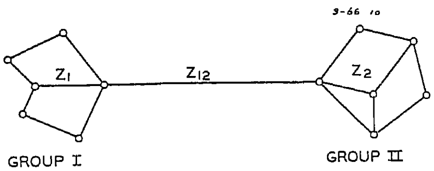

Figure 10. Two closely coupled groups of synchronous power plants, loosely interconnected by tie line of high reactance

In our former example of three different generator units of 5, 10, and 20 megawatts rated power, the distribution among these machines of the power oscillations of frequency ν_0 would be, according to equation 53,

$$\left.\begin{array}{l} P_1 = 8\cdot 5(0.686^2 - 1.12^2) = -40\cdot 0.78 \\ \quad = -31.2\cdot\dfrac{A_n}{\omega} \\ P_2 = 5\cdot 10(0.686^2 - 2.58^2 = -50\cdot 6.19 \\ \quad = -309\cdot\dfrac{A_n}{\omega} \\ P_3 = 20\cdot 20(0.686^2 - 0.454^2) = 400\cdot 0.264 \\ \quad = +106\cdot\dfrac{A_n}{\omega} \end{array}\right\}$$

These figures show a disproportionate subdivision in magnitude as well as in sign, the medium machine taking over the bulk of the power oscillations, and the large unit acting in phase opposition to the smaller machines. The entire synchronous generator power oscillates with an amplitude

$$P_g = 106 - 309 - 31 = -234\cdot\frac{A_n}{\omega}$$

indicating an extremely unfavorable distribution among the generators. On the other hand, the entire motor power oscillation within this system is, according to equation 51, and with use of equation 49,

$$P_m = \nu\frac{\nu A_n}{\omega}\Sigma S_m = 0.686^2\cdot 500\frac{A_n}{\omega} = 236\cdot\frac{A_n}{\omega}$$

checking closely the total generator value.

If the number of generator units and motors changes, as is the case with varying conditions of operation of all electric systems, the value of ν_0 in equation 53 and in Figure 9 may change over a certain range, with the result that another generator may become more overloaded than before. Always the units with the smallest $\delta T_c T_s$ are predominantly endangered. For the three machines of our example, this characteristic value is $\delta T_c T_s = 5\cdot 2\cdot 8 = 80$ and $3\cdot 1\cdot 5 = 15$ and $6\cdot 4\cdot 20 = 480$. Widely different inherent governor frequencies causing unbalanced load distribution in the transient state of the system are especially found if steam and water power plants are interconnected.

Under one condition only can the overloading of one or another machine be avoided and the subdivision of the oscillating power among all machines remain the same under all circumstances, namely, if we tune the various speed-governing mechanisms so that all give one and the same inherent value γ for every machine. Then, all the frequency differences from any joint frequency ν_0 in equation 53 or Figure 9 attain the same magnitude. Even if the value of ν_0 should change because of a varying number of generators or motors in operation, see equation 47, the frequency differences for the various generators still remain equal between each other. Therefore the synchronous power oscillations of the generators, according to equation 53, are merely proportional to their inherent inertias S_1, S_2, and so forth, a subdivision corresponding to the mechanical size of each machine. Equal γ, according to equation 7, leads to the condition

$$\delta_1 T_{c1} T_{s1} = \delta_2 T_{c2} T_{s2} = \delta_3 T_{c3} T_{s3} \ldots = \text{constant} \qquad (54)$$

for every power unit consisting of synchronous generator, prime mover, and governor. This requires a tuning of the speed-governing mechanism with respect to the starting time constant T_s for each machine, which in general can be attained. Such a system will have the greatest possible stability against too large joint oscillations, so that no generator will be overloaded or even lose synchronism with the other generators after a heavy impact of any type on the network.

5. Supplementary Oscillations

There may be examples in which the reduction of the electrical distribution network into a simple star system cannot be performed, either in perfect or in faulty state of the system. In such cases two or more loosely coupled star systems may give a proper approximation. Since all oscillations here treated are due to the energy storage in the various rotating masses, we see immediately that the number of the natural frequencies will not be changed; only the spectral distribution may shift to some degree. If, for example, two groups of machines, each comprising generators and motors, are closely coupled within the groups but loosely between the groups, as in Figure 10, these two groups will be able to oscillate against each other, and the corresponding natural group frequency, dependent preponderantly on the value of the coupling impedance, can be much lower than any individual inherent frequency σ. The general scheme of deriving the actual frequencies of such networks can closely follow the procedure of section 3*A*.

If the coupling of such network groups is so loose that a resultant natural frequency of the electrical machines drops into the region of the slow governor oscillations, even these may be affected and a beating of these two low frequencies may reduce the oscillatory stability of the entire system.

Some types of speed governors do not follow with sufficient accuracy a relation like equation 4 but possess more complex characteristics caused, for example, by droop compensation, by use of intermediate pilot valves, by long water or fuel supply pipes, and so forth. In every case, the equations of operation of the governors and prime movers can be combined, similar to the manner shown in section 3*A*, with those of the electrical system which remain unchanged. As a result, there may appear some additional natural frequencies, attributable for example, to frequencies inherent in the governor systems, which sometimes are much higher than the preponderant low governor frequency γ and which therefore may enter the range of the synchronous-machine frequencies σ. Since both these types belong to natural oscillations and not to forced oscillations, no possible resonance effects can occur. Only the spectrum of the natural frequencies will change to some extent, not causing, however, any additional disturbances.

In large distribution systems, particularly with interconnected networks, the frequency of the entire system, or the power distribution among the different branches, is sometimes controlled by an additional electromechanical device. No standard equipment or customary operation of such superposed control system has been developed as yet, but with all of them the deviation of frequency, angle, or power from the desired values at certain points in the system serves to induce one or more prime movers by influencing their speed governors to compensate for the difference.

In the light of our analysis, such devices introduce another set of restoring powers into the system, very similar to the restoring power of equation 4, and additional oscillations may result. If such superposed control is restricted to one or to some of the generator units only, the restoring power for the entire interconnected system is relatively small compared with that of all speed-governed prime movers together. Therefore the frequency of oscillation for such a scheme of control, in analogy to equation 47, will be still much lower than that of the joint governor oscillations, and usually these very slow power swings are slightly damped by internal forces in the additional control device.

Since such control frequency is always much lower than the electrical frequencies σ and μ between the machines, all the rotating masses will follow in unison, and thus we can use equation 47 for the determination of the control frequency, if only the total power velocity of control in kilowatts per second of the control device is given. Hence every program control of any number of machines will always affect the inertia masses of the entire system as if they were rigidly coupled to a unit.

6. Damping Effects

We have considered the interaction of all the coupled oscillating elements, neglecting their damping forces. Let us supplement the results by discussing qualitatively the influence of these dissipative effects.

With inclusion of the various damping members, the differential equation 31 for the individual angles of deviation θ of the synchronous machines, as derived from equation 30 would contain terms with $d\theta/dt$. The first such term, because of the governor damping, originates from equation 4 by substituting the slip from equation 14 and integrating:

$$N + \int \frac{N\,dt}{T_c} = \frac{N_0}{\delta T_c} \, \frac{\theta + \theta_n}{\omega} \qquad (55)$$

Substituting T_c on the left-hand side from equation 8, we obtain for a power oscillation of any given frequency ν,

$$N = \frac{N_0}{\delta T_c} \cdot \frac{\theta + \theta_n}{\omega} \cdot \frac{1 + j\,\dfrac{\rho_\gamma}{\nu}}{1 - \left(\dfrac{\rho_\gamma}{\nu}\right)^2} \qquad (56)$$

if we shift the complex factor, resulting from the integral, to the right-hand side.

Comparing this with the first term of equations 30 and 31, the last quotient of equation 56 results in a slight increase in N by effect of the denominator and in a damping effect for every prime-mover unit by action of the imaginary term $j\rho_\gamma/\nu$. Since each governor furnishes such damping term in the oscillations of the entire system, the low common governor oscillation with frequency ν_0 will suffer a damping effect which is determined by the average ρ_γ/ν_0 of all the interconnected prime movers. Since ν_0 is decreased by the large inertias of the induction motors in the system, the resultant damping is heavier than with the generators alone. However, the oscillations usually remain within the periodic region.

Since the governor damping is inversely proportional to the frequency ν of the oscillation considered, this effect vanishes almost completely for the much faster oscillations with frequencies near σ of the synchronous generators and near μ of the induction motors.

The damping in the synchronous machines is due to the last term of equation 10. For any given frequency ν we have, therefore, for every generator unit the electric power oscillation

$$P=\left(p_s+jp_a\frac{\nu}{\omega}\right)\theta=p_s\left(1+j\frac{\nu p_\sigma}{\sigma^2}\right)\theta \qquad (57)$$

equations 17 and 18 being used for the value of p_a/p_s.

This expression enters equations 30 and 31 in place of the last term, producing a damping effect from every individual generator by action of the imaginary term. Since the natural frequencies ν for the entire group of generators are all in the same range, these damping terms of the synchronous units all act with a similar strength and after some time produce decay in all the natural generator oscillations. For the slow governor oscillations ν_0, however, the damping term of equation 57 becomes extremely small, being proportional to the frequency ν, and thus the electric damping in the synchronous machines has practically no influence on the slow power oscillations caused by the governors. The motor oscillations with higher natural frequencies, however, are more strongly influenced in the proportion of the μ range to the σ range.

The damping effect of the slip in induction motors is given by the second term of equation 23. It constitutes a correction of the active power of the first term of that equation, which is for any frequency ν,

$$\begin{aligned} P+s_0T_s\frac{dP}{dt}&=P(1+j\nu s_0T_s)\\ &=P\left(1+j\frac{\nu p_\mu}{\mu^2}\right) \qquad (58)\end{aligned}$$

equations 25 and 26 being used for the value of s_0T_s. The imaginary term for every motor causes a damping of all the natural motor oscillations, the effect of the various induction motors being greater the nearer the damping comes to the aperiodic condition of equation 26. Since it is not possible to co-ordinate an actual natural frequency to a single motor, see Figure 7, the damping effect of every motor spreads out over the whole group. The motor damping, according to equation 58, is proportional to ν, and thus its influence on the much slower generator oscillations is small, and particularly that on the governor oscillations is negligible.

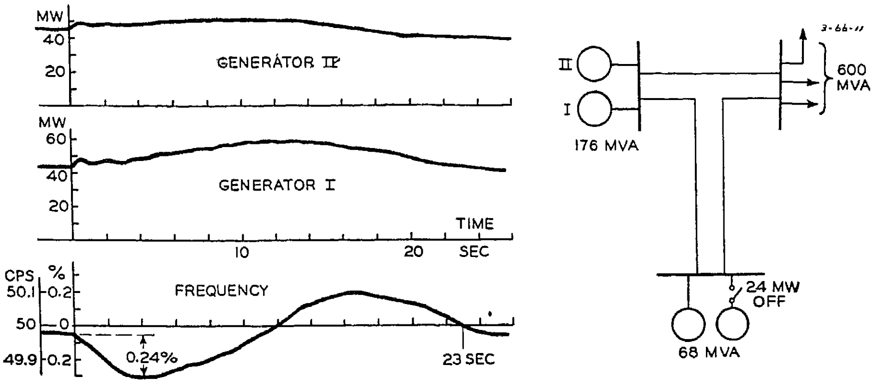

Figure 11. Test on 800-megavolt-ampere system, showing the various frequencies of power oscillations after heavy impact, caused by disconnecting one of feeding generators

Hence the damping effects of governor, generator, and motor oscillations remain in their respective ranges, with the exception only of the generator damping, which overlaps the motor range. The motor oscillations proper are damped, therefore, by two effects, both of which are considerable, and thus are not to play an important part under conditions of unbalance of the system after any disturbance. Individual generator oscillations and joint governor oscillations are the important phenomena which determine the stability of interconnected systems, while all the motors merely act together with the combined inertia of their rotating masses.

In all our derivations we have assumed that the magnitudes of the generator voltages remain constant during any oscillation and that the influence of the network resistances is insignificant, the reactances playing the predominant part. It is well known, however, that even with constant excitation of the synchronous generators, their voltage varies proportionally to the instantaneous speed and, therefore, oscillates to some extent, and that this variation gives rise to a corresponding oscillation of power losses within the resistances of the network, series resistance of the lines as well as parallel resistance of the loads. Thus a network damping occurs, additional to the damping effects within the active elements just considered, the value of which depends largely upon the distribution of resistance-, motor-, and arc-consumers within the system. Since such network damping produces merely a corrective effect which can be either positive or negative, it will not be further considered here. Similarly, the resistances and pressure drops within the steam, water, or fuel system, increasing with the load, cause an additional damping which also is of minor significance for the value of the natural frequencies and will be neglected here.

Any arbitrary change of voltage in one or more of the generators by external change of their excitation acts more on the reactive than on the active power of the machines and therefore influences more the value of the synchronizing forces, see equations 11 and 17, than the damping effects. It therefore merely causes the frequencies to vary slowly during subsequent oscillations.

The absolute amplitudes to which all the oscillations are excited by sudden impacts or by arbitrary or accidental changes in the system remain to be investigated.

Figure 11 shows results of a full-scale experiment with an interconnected system containing scores of generators, totaling a rated power of 800 megavolt-amperes, which was subjected to a power impact of minus 24 megawatts, attained by sudden disconnection of one of the feeding generators. The network frequency in consequence shows a damped oscillation of a period of 23 seconds, corresponding to $\nu_0=0.274$, caused by the combined action of the speed governors in the system. The relative amplitude of this frequency oscillation is $F=-0.24$ per cent of the normal frequency. Two generators measured in a distant power plant show a slow power oscillation of the same frequency but of different amplitudes and, in addition, a more rapid oscillation for each of them in the beginning, the periods of which are 1.57 and 1.40 seconds, respectively, and which are due to the different synchronous forces of these individual machines.

7. Conclusions

1. Notwithstanding the complexity of the problem of interrelation of the many tran-

sient electrical and mechanical forces existing in widely interconnected systems, a simple solution can be given for the natural oscillations of frequency and of power which occur, by discriminating between a small number of significant major effects and the numerous minor effects of merely corrective value.

2. For evaluating this solution numerically, only a few parameters of all the machines have to be known, namely: the inherent natural frequency of the speed-control system of every prime mover, determined by droop, valve-closing time, and starting time constant; the inherent natural frequency of every synchronous generator, determined by network frequency, synchronizing coefficient and the same value of starting time constant; and, although not necessarily, the inherent frequency of the larger induction motors, determined by leakage voltage and their starting time constant.

3. All of the many rotating machines in an interconnected system are coupled by the electric energy which they can exchange in the transient state. Thus they perform coupled oscillations, the frequencies of which are different from those inherent in the single machines when not connected together.

4. The characteristic equation for the determination of the coupling frequencies is of a very high degree, given by the number of oscillatory elements, namely, the total number of prime movers with generators, and of motors, in the entire system. An analytical solution of such an equation is not practicable, but a graphical representation can be developed, giving an excellent survey of the dependence of the frequencies and the power amplitudes on the various parameters.

5. There exists no isolated oscillation of any one machine with respect to a large network. All oscillations, although they may be excited individually, spread out over the entire network affecting every line and every rotating machine.

6. The natural frequency of oscillation of every synchronous generator is increased by the reaction of the mechanically coupled speed governor of its prime mover, and by the electric coupling through the network with other rotating machines.

7. Tuning of the inherent frequencies of all the synchronous generators to the same value, accomplished by proper distribution of the reactive power among the generators insures their oscillating in unison during the transient state and results in maximum stability of the entire system.

8. A single induction motor is capable of natural oscillations of moderate frequency if the slip at rated load is under a certain limiting value. For larger rated slip, aperiodic gliding movements occur in the transient state.

9. The limiting rated slip of induction motors, separating their aperiodic and oscillatory behavior, is determined by network frequency, relative leakage voltage and starting time constant. Since the limiting slip is of an average value of five per cent, small motors perform aperiodically while large motors may oscillate.

10. The natural oscillations of induction motors fed from the same network interfere strongly with one another by effect of their electric coupling. However, these oscillations are heavily damped and therefore are not very significant for the system performance.

11. For slower oscillations than those in their natural frequency, induction motors in the transient state act preponderantly by their inertia masses and only slightly by their slip and leakage effects.

12. The rotating masses of all the induction motors taken together in interconnected networks influence strongly the oscillations of the generators and speed governors.

13. All the speed governors taken together constitute a unit of restoring forces, acting not only on the inertia masses of all the prime movers and generators, but also on the inertia masses of all the induction motors and other rotating machines, all in unison.

14. Independent of the number of machines in the interconnected networks, there develops only a single low frequency by action of all the speed governors, this frequency being given merely by the sums of all the restoring forces and all the rotating masses of the system.

15. Although all the governors oscillate in unison at very low frequency, the electric power exchange of their generators with the interconnected system may be entirely disproportionate, the units with low inertia, small droop and low valve-closing time being overloaded, while those with large values may even be negatively loaded by the oscillations, thus driving their inertia masses as synchronous motors.

16. Tuning of the inherent frequencies of all the speed governors to the same value, secured by adapting the properties of the speed-control mechanism of each prime mover to the inertia of its rotating masses, insures a proportionate distribution of power in the transient state and results in maximum stability of the entire system.

17. Such tuning requires that the product of speed-governor droop, valve-closing time, and starting-time constant be the same for every generator unit. Thus heavy machines with relatively high mass inertia should be controlled by fast servomechanisms and speed governors of small droop, while relatively light machines should have slow servomechanisms and larger governor droop.

18. In tuned conditions of governors and of generators, the distribution of power oscillations among the generator units corresponds to the inertia of their rotating masses which usually are in proportion to the mechanical and electrical size of the machines.

19. Oscillations of power and frequency produced by the action of any type of power or frequency program of control, superimposed on a limited number of prime movers, have a very low frequency on account of the relatively weak restoring forces acting on the inertia masses of the entire system.

20. By effect of the various restoring forces, three main groups of oscillations prevail in interconnected systems, influencing one another but being distinguished by their frequency ranges; namely, a motor range of 5 to 10 cycles per second; a generator range of 1 to 2 cycles per second; and a governor range of 0.05 to 0.1 cycles per second.

21. The entire number of frequencies to be expected in interconnected networks under perfect conditions with all machines different, is in the governor range equal to one, in the generator range equal to the number of synchronous generators, and in the motor range equal to the number minus one of oscillating motors.

22. For power systems loosely interconnected by high reactance lines and for power systems under faulty condition of their lines, one or more of the medium range generator frequencies may shift into the low range of the governor frequency.

23. Records of full-scale tests on large interconnected power systems while suddenly imposing a severe power impact, show very slow frequency oscillations at the bus bars, and faster power oscillations of the generators, consistent with the foregoing conclusions.

24. All the natural oscillations suffer a continuous damping, preponderantly caused by the effect of the return of the speed governors, the damper of the synchronous generators, and the slip of the induction motors.

25. The damping of the slow governor oscillation of the entire system depends upon the average damping force of the returns of all the speed governors and is considerably increased by the effect of the rotating motor masses, but not so much as to become aperiodic.

26. The damping of the faster synchronous generator oscillations throughout the system depends preponderantly upon the average damping force of all the damper cages in the generators and always remains in the periodic range.

27. The damping of the still faster induction motor oscillations throughout the system depends upon the joint effects of the slip in the motors and the dampers in the generators. Smaller motors with considerable slip glide in the aperiodic range, while larger motors with small slip oscillate in the periodic range, but with heavy decay of their amplitudes.

8. Summary

Electric generating and distribution systems are capable of performing natural oscillations of frequency and of power, superposed upon the ordinary operation and sometimes disturbing the regular behavior of the system or its parts. These oscillations are due to the interlinked cooperation of the prime movers with their speed-governing mechanisms, the synchronous generators, the ramified network, and the numerous induction motors, and similar power consumers. Since most of the machines and network branches usually are different in size and in individual design, a multitude of natural oscillations will develop, the frequency spectrum of which can be divided into three groups.

In perfect condition of the system, a single very low frequency develops, which is determined by the inherent properties of all speed governors and servomotors together, and by the inertias of the rotating masses of all the generators and motors together. This joint natural oscillation has a period around 10 to 20 seconds and causes an inphase movement of all mechanical parts of the system, which can be of considerable magnitude, while the electric power may oscillate in phase opposition within the various parts of the system.

The next group of frequencies contains as many natural oscillations as there are different synchronous machines in the system and is defined by the faster electromechanical oscillations occurring between each synchronous generator and the entirety of all the other synchronous and induction machines. The mechanical parts of the entire power system oscillate here in phase opposition at all the different frequencies which have periods of about 0.5 to 1 second and are of moderate amplitude as long as the system is in perfect condition.

The third and highest group of natural frequencies is caused by the different inertia effects of the various induction motors, all oscillating against each other with periods around 0.1 to 0.2 seconds. The amplitudes are usually insignificant since they are damped by the effect of the rotor slip of each machine, for small motors even aperiodically.

During every transient state of the system, such oscillations produce slow and fast exchanges of power between the various parts of the network, temporarily increasing the load on some machines or in some tie lines and decreasing it on others, which often is detrimental to good operation. However, certain tuning conditions are derived under which the amplitudes of all these oscillations reduce to a minimum, thereby securing the highest possible stability of the entire system after any disturbance.

Tests during the operation of a large system of interconnected networks after a severe impact show frequency and power oscillations consistent with the derivations in this paper.

Special Symbols

A = angular amplitude
F = frequency amplitude
N = mechanical power
P = electric power
S = inertia constant
T = time constant
e = relative voltage
g = gravity constant
$j = \sqrt{-1}$
k = synchronizing coefficient
p = specific power
γ = governor frequency
δ = droop of governor
θ = pole angle
μ = motor frequency
ν = natural frequency
σ = synchronous generator frequency
ρ = damping factor

References

1. SYNCHRONIZED AT THE LOAD—II; CALCULATIONS OF SYSTEM PERFORMANCE, **S. B. Griscom.** AIEE TRANSACTIONS, volume 48, 1929, pages 1083–92.

2. HYDRAULIC TURBINE GOVERNORS AND FREQUENCY CONTROL, committee report. *Proceedings*, National Electric Light Association, volume 88, 1931, page 542.

3. FREQUENCY AND TIE-LINE LOAD CONTROL, **Philip Sporn, V. M. Marquis.** *Electrical World*, volume 99, 1932, page 618.

4. GEMEINSCHAFTSSCHWINGUNGEN GEKUPPELTER SYNCHRONKRAFTWERKE MIT ASYNCHRONEN NETZEN, **Reinhold Rüdenberg.** *Wissenschaftliche Veröffentlichungen aus dem Siemens-Konzern* (Germany), volume 11.1, 1932, page 69.

5. CONSTANT SYSTEM SPEED AND THE STEAM-TURBINE GOVERNOR, **A. F. Schwendner.** American Society of Mechanical Engineers, *Transactions*, volume 62, 1940, page 199.

6. THE FIELD OF SYSTEM GOVERNING, **A. Davis.** American Society of Mechanical Engineers *Transactions*, volume 62, 1940, page 207.

7. REGULATION OF SYSTEM LOAD AND FREQUENCY, **H. Estrada, H. A. Dryar.** American Society of Mechanical Engineers *Transactions*, volume 62, 1940, page 221.

8. PRIME-MOVER SPEED GOVERNORS FOR INTERCONNECTED SYSTEMS, **R. J. Caughey, J. B. McClure.** AIEE TRANSACTIONS, volume 60, 1941, April section, pages 147–51.

9. SYSTEM LOAD SWINGS, **H. A. Bauman, O. W. Manz, Jr., J. E. McCormack, H. B. Seeley.** AIEE TRANSACTIONS, volume 60, 1941, pages 541–7.

10. EFFECT OF PRIME-MOVER SPEED-GOVERNOR CHARACTERISTICS ON POWER-SYSTEM FREQUENCY VARIATIONS AND TIE-LINE POWER SWINGS, **C. Concordia, S. B. Crary, E. E. Parker.** AIEE TRANSACTIONS, volume 60, 1941, pages 559–67.

11. POWER SYSTEM GOVERNING—THE PROBLEM, **J. J. Dougherty, A. P. Hayward, A. C. Monteith, S. B. Griscom.** AIEE TRANSACTIONS, volume 60, 1941, pages 547–58.

12. A TURBINE-GOVERNOR PERFORMANCE ANALYZER, **W. C. Osbon.** AIEE TRANSACTIONS, volume 61, 1941, November section, pages 963–7.

13. TRANSIENT OVERSPEEDING OF INDUCTION MOTORS, **R. W. Ager.** AIEE TRANSACTIONS, volume 60, 1941, December section, pages 1030–2.

14. CONTROL OF TIE-LINE POWER SWINGS, **C. Concordia, H. S. Shott, C. N. Weygandt.** AIEE TRANSACTIONS, volume 61, 1942, June section, pages 306–14.

15. FREQUENCY CONTROL OF LOAD SWINGS, **J. E. McCormack, R. J. Lombard.** AIEE TRANSACTIONS, volume 61, 1942, September section, pages 623–4.

The Sorocabana Railway Electrification

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Synopsis: The Sorocabana Railway electrification is noteworthy in that the railroad is a meter-gauge line run largely on wood fuel. The lack of suitable national coal or oil sources and the increasing difficulty of obtaining wood fuel led the railway to electrify its line between São Paulo and Santo Antonio.

Rolling stock is outstanding in that the locomotives will be the most powerful 3,000-volt narrow-gauge units ever built. The multiple-unit cars are the first 3,000-volt meter-gauge units to be used.

The distribution system is designed to minimize the use of steel supports. Steel must be imported, and locally made concrete poles are to be utilized extensively.

The power-supply system using 3,000-volt mercury-arc rectifiers is the first installation in this hemisphere to use rectifier conversion with regenerative braking on locomotives. Regenerated energy returned to the substations will be dissipated in resistors roof-mounted at the several stations.

THE Sorocabana Railway—the EFS (Estrada de Ferro Sorocabana) as it is commonly known—consists of a network of meter-gauge lines traversing the southern and southwestern parts of the state of São Paulo in Brazil. The system is confined entirely within the state.

The network is composed of two principal lines:

(*a*). The east–west line, which extends 556 miles from the city of São Paulo westward to Presidente Epitacio, a small town on the Paraná River, the border line between the states of São Paulo and Matto Grosso

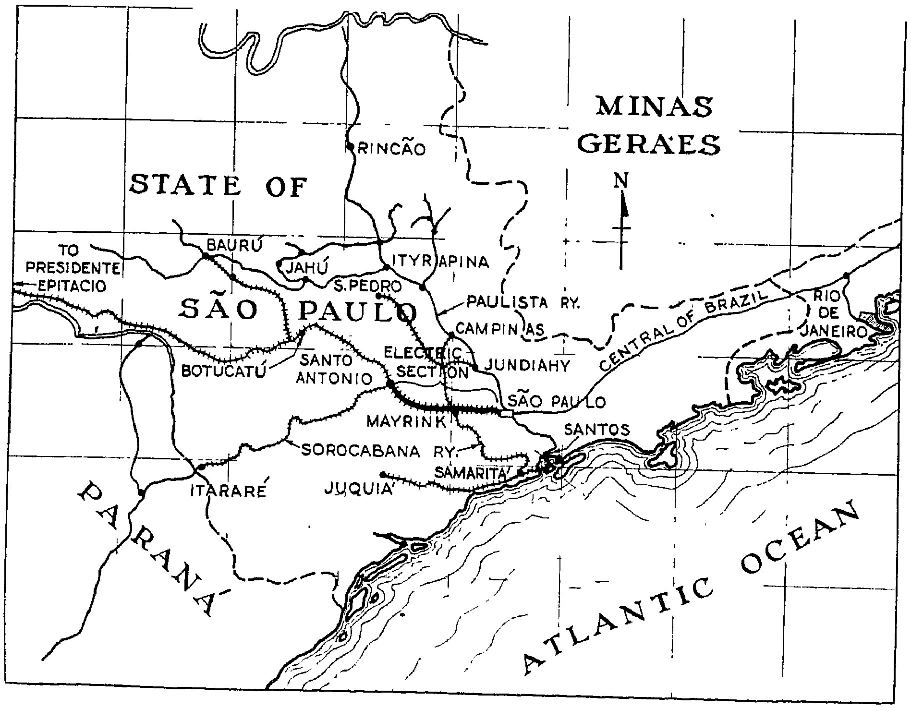
(*b*). The north–south line, which runs from the port of Santos to San Pedro—a distance of 238 miles—crossing the east–west line at Mayrink, a junction point approximately 43 miles west of São Paulo

and the several branch lines emanating from these main arteries as shown in the sketch map of Figure 1.

The two principal lines account for 794 miles of the total line trackage of 1,340 miles. All of the line trackage is single with the exception of that portion between São Paulo and Santo Antonio on the east–west line. This 87-mile-long section is double-tracked and is the territory selected for the initial application of electric traction.

Figure 2 is a condensed profile of the line now being electrified, and Figure 3 shows the profile of the line between sea level and the junction of the north–south and east–west lines at Mayrink. Orientation of the two profiles by means of the station (Mayrink) common to both will indicate the elevated location with respect to sea level of the São Paulo–Santo Antonio section.

Figure 1. Sketch map showing Sorocabana Railway lines



Although the railway is owned by the state of São Paulo, it is managed and operated as a practically autonomous institution responsible to the State Secretary of Transportation and Public Works. In gross income, the system is the second largest in Brazil and is exceeded only by the federally owned and operated Central Railway of Brazil.

The predominant factor influencing the decision to electrify is that of locomotive fuel supply. Brazil has no developed sources of fuel oil. Native coal, because of its low Btu content, is not entirely satisfactory as a locomotive fuel. Not only is the coal of a poor quality, but sufficient coal to meet all of the country's requirements is not mined—a state of development attributable to the poor quality of the coal. Steam-locomotive fuels, alternative to native coal, are imported coal and oil and native wood.

The long hauls between the nearest oil fields and the points of use in Brazil make the use of oil as locomotive fuel economically prohibitive.

Many of the railroads employ English coal in normal times and, since the incidence of the present world-wide war, coal imported from the United States.

In 1939 the Sorocabana used 6,500 tons of imported coal, 6,015 tons of native coal and 502,000 cords (128 cubic feet per cord) of wood* at a total cost of approximately $1,593,000. This represents 30 per cent of the railway's total operating expenses. Although the comparison may not be strictly balanced, it is of interest to note that during the year 1939 fuel expenditures for railroads in the United States averaged but nine per cent of the total operating expenses.

Because of its relatively low heating value, wood fuel requires the handling of large volumes, compared to coal, and on the Sorocabana there were assigned to nonrevenue company fuel-haulage service 25 locomotives and 360 flatcars—figures which represent eight per cent of the total motive power and 20 per cent of the flatcars owned.

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* 1 cord of wood = 1,000 pounds of Cardiff coal on an equivalent heating basis

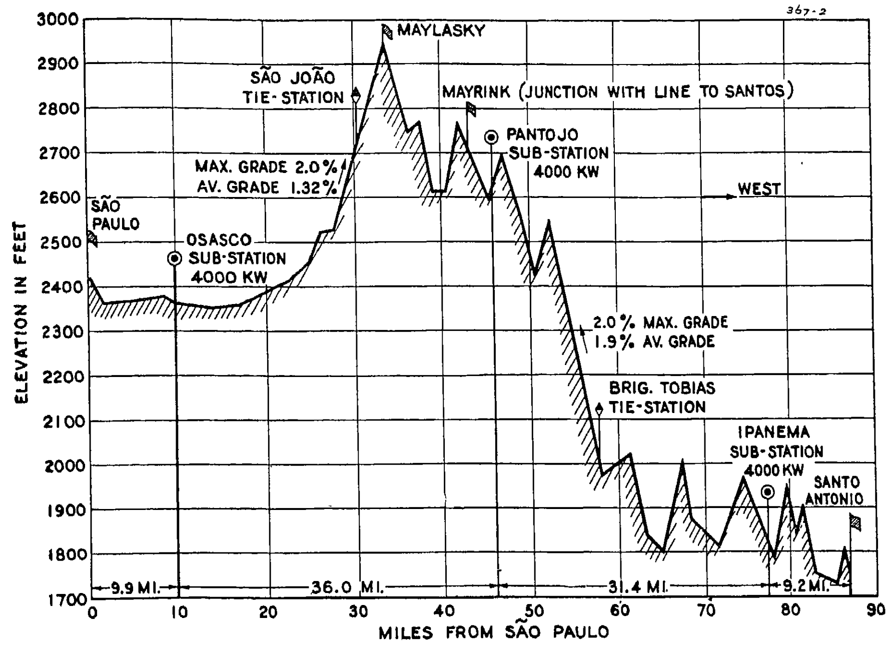


Figure 2. Condensed compensated profile—Sorocabana Railway—São Paulo to Santo Antonio double-track meter gauge

A factor which further aggravates the fuel-supply situation is that of increasing scarcity of suitable wood supplies. Excluding that section of the railroad in the extreme western part of the state, fire wood supplies contiguous to the main-lines are inadequate, and it has been found necessary to construct branch lines—some of these are 15 miles in length—expressly for wood-fuel haulage.

That the fuel-supply problem eventually would become critical has been recognized for some time, and as far back as 1924 electrification of various sections of the line was studied. In the interim, traffic has continued to increase in pace with the expansion and development experienced by the territory traversed. Preliminary studies indicated the economic desirability of electrifying the main east–west line between São Paulo and Botucatú and the north–south line from Samaritá (12 miles from Santos) to Mayrink. Further studies developed the feasibility of electrifying initially the double-tracked portion of the east–west line between Sao Paulo and Santo Antonio—since this section carries the greatest traffic density—leaving the Samaritá-Mayrink and the Santo Antonio–Botucatú sections for future electrification extension.

Hydroelectric power is relatively abundant and cheap in the territory served by the section to be electrified and for these reasons seems to be the logical solution for the difficult fuel problem.

Economics of Electrification

In studying the economic feasibility of electrifying the São Paulo–Santo Antonio line, the commission appointed by the state selected as a basis for its calculations the average traffic conditions expected to hold for the ten-year period 1942–1952, that is, the conditions corresponding to those anticipated for the year 1947.

The railway's records for the year 1939 show the performance for the São Paulo–Santo Antonio section indicated in Table I.

Statistics for the period 1928–1938 show that the increase of passenger and mixed trains, as compared with freight trains, has been small. During this period the annual traffic increase for the whole railway system averaged 6.2 per cent. It will be well to note that during these ten years three abnormal periods were experienced—the economic crisis starting in 1929, and the civil disturbances occurring in the years 1930 and 1932—and these served to lower the traffic index figures to 92, 97, and 94, considering the 1928 index as 100. Because of this, the average rate of increase in freight-train traffic for the period 1942–1952 has been taken at seven per cent, and on this basis the anticipated performance for the year 1947 will be 56 per cent greater than that of 1939.

In forecasting passenger-train performance, it was assumed that the use of electric locomotives more powerful than existing steam units would permit maintaining the same number of train-miles as in 1939, at the same time increasing the tonnage hauled by the addition of cars to both through and suburban trains.

The projected 1947 figures appeared as shown in Table II.

STEAM OPERATING COSTS

Unit steam operating costs were based on 1938 figures, since those for 1939 were not available at the time the study was under way. Wages involved in the operation of various classes of trains per 1,000 train-miles during 1938 are shown in Table III. The average cost of locomotive repair and maintenance amounted to $31.50 per 1,000 locomotive-miles. The average cost of fuel per 1,000 gross ton-miles of traffic amounted in 1938 to $1.485.

The average costs of fuel prevailing during 1939 is shown in Table IV.

Water costs amounted to an average of 2.3 cents per 1,000 gross ton-miles.

Lubricant (oil and waste) costs amounted to $3.86 per 1,000 locomotive-miles.

The aforementioned unit costs being combined with the projected traffic figures, the cost of operating the São Paulo–Santo Antonio line with steam motive power would amount to—so far as the items to be affected by electric operation are concerned—the figures shown in Table V. Locomotive-miles are assumed to be equal to train-miles.

ELECTRIC OPERATION

Unit cost data utilized for estimating the expenses expected under electric operation are based on statistics furnished by the Central Railway of Brazil, which operates an extensive multiple-unit-car suburban service out of Rio de Janeiro, and by the Paulista Company—in the case of passenger-, mixed-, and freight-train operation—which operates a 3,000-volt main-line electrification in Brazil.

In estimating the performance with electric traction, the following assumptions have been made:

1. That the through passenger service will be identical with that under steam traction.

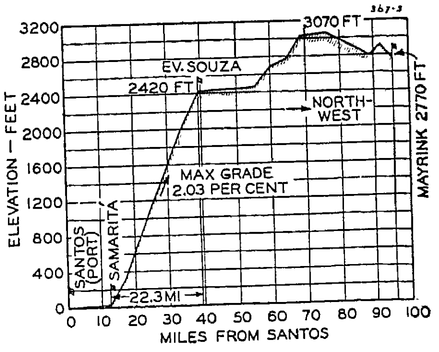


Figure 3. Condensed profile—Sorocabana Railway—Santos to Mayrink

Table I. Performance of São Paulo–Santo Antonio Section of Sorocabana Railway, 1939

	Train-Miles	1,000 GTM*
Passenger	273,000	78,500
Suburban	181,200	24,400
Mixed	128,300	25,800
Freight	1,042,000	366,200
Totals	1,624,500	494,900

* Gross ton-miles, not including locomotives.

Table II. Anticipated Performance of São Paulo–Santo Antonio Section of Sorocabana Railway, 1947

	Train-Miles	1,000 GTM
Passenger	273,000	100,500
Suburban	181,000	31,200
Mixed	128,300	36,150
Freight	1,623,000	572,000
Totals	2,205,300	739,850

Table III. Wages Involved in Operation of Trains Per 1,000 Train-Miles, 1938

Class of Train	Operating	Repairs
Passenger and suburban	$16.26	$33.10
Mixed	$22.40	$21.85
Freight	$30.05	$28.15

Table IV. Average Costs of Fuel, 1939

Wood	$ 2.37 per cord (128 cubic feet)
Imported coal	$13.63 per ton (2,000 pounds)
Brazilian coal	$ 5.90 per ton (2,000 pounds)

2. That suburban trains out of São Paulo will perform the same number of train-miles, but the ton-miles involved will be reduced from 31,200,000 to 26,650,000, because of the difference in dead weight between the steam-hauled cars and the multiple-unit cars.

3. The mixed-trains data are based on an assumption of six trains daily between Barra Funda (the São Paulo yards) and Sorocaba, and four trains between Sorocaba and Santo Antonio, with a total of 170,500 train-miles and 61,500,000 gross ton-miles.

4. In the case of the freight trains, it has been assumed that the tractive utilization of the electric locomotives will be 90 per cent, and the average train tonnage will be approximately 520 tons. For the same quantity of ton-miles as was assumed for steam operation, that is, 572,000,000, the train-miles will amount to 1,100,000.

In summary the traffic under electric operation in 1947 is expected to be as indicated in Table VI.

ELECTRIC OPERATING COSTS

With electric traction, schedule times will be reduced considerably because of the increased operating speeds available with the new motive-power equipment. These reductions vary with the class of service: for passenger trains 23 per cent; for suburban trains, 25 per cent; for mixed trains, 13 per cent and for freight trains, 25 per cent. Since certain expenses are irreducible, regardless of the type of motive power, the crew wages and repair wages under electric traction will be calculated on the basis that but 90 per cent of these reductions will be realized. Labor costs per 1,000 train-miles will then amount to the figures shown in Table VII.

Electric-locomotive maintenance costs are prorated from actual cost data supplied by the Paulista Company lines. The average cost of labor and maintenance on electric locomotives on these lines during the years 1935–1937 was 1.385 cents per locomotive-mile. During the same period, the average cost of labor and maintenance for steam locomotives on the Paulista Company meter-gauge lines was 1.85 cents per locomotive-mile. If this ratio is assumed to prevail on the Sorocabana Railway lines, the cost of labor and maintenance on electric locomotives will be

$$\$31.50\times\frac{1.385}{1.85}=\$23.55/1{,}000\text{ locomotive-miles}$$

For the suburban trains, it is assumed that the cost of maintenance on the mechanical portions of the cars will be the same as under steam operation. Consideration will be given, therefore, only to maintenance costs on the electrical equipment. The unit cost is taken as that found in the years 1938–1939 for multiple-unit cars in suburban service on the Central Railway of Brazil. The cost amounts to $7.08 per 1,000 train-miles—the train consisting of a trailer–motor–trailer combination similar to that to be used in the São Paulo suburban service on the Sorocabana Railway.

ELECTRIC-POWER COSTS

Energy consumption and consequent power costs have been based on detailed calculations, taking account of the class of rolling stock and the profile to be operated, for all except the multiple-unit train service. Actual performance figures, modified to allow for longer runs, fewer accelerations and lighter trains, for the multiple-unit service on the Central Railway of Brazil, have been used for the Sorocabana Railway suburban service. This amounts to 50.1 watt-hours per ton-mile. Expressed in terms of gross ton-miles not including locomotives, and considering the power at the motive-power pantographs, these values are as given in Table VIII. With an efficiency of conversion and distribution at 87 per cent, energy consumption for the various services, without taking into account regenerated energy, will be as shown in Table IX.

The maximum demand taken from a plot of power demand against time for a train sheet representing a maximum day was calculated at 11,200 kilowatts measured at the a-c incoming lines.

Regenerated energy according to calculations will amount to approximately 16 per cent. With this amount allowed for in all services excepting the suburban, the total purchased energy will come to 37,259,000 kilowatt-hours annually.

Based on the contract signed by the Central Railway of Brazil with the Rio de Janeiro Tramway Light and Power Company, the cost of power for the Sorocabana Railway would be on a monthly basis

$$\begin{aligned}&11{,}200\text{ kilowatts}\times\$0.90 &&=\$10{,}080.00\\&\frac{37{,}259{,}000\text{ kilowatt-hours}}{12}\times\$0.00105 &&=\underline{\$\ 3{,}260.00}\\& &&\ \ \$13{,}340.00\end{aligned}$$

To this a two per cent tax for retirement funds is added, bringing the total monthly power bill to $13,607.

The average cost per kilowatt-hour will amount to, on this basis,

$$\frac{\$13{,}607\times12}{37{,}259{,}000}=\$0.00439\text{ per kilowatt-hour}$$

Power charges attributable to the several services per annum—after allowing for regeneration—will amount to the figures given in Table X.

The cost of lubricating oil, grease, and

Table V. Cost of Operating São Paulo–Santo Antonio Line With Steam Motive Power

Type of Service	Passenger and Suburban	Mixed Trains	Freight Trains	Totals
Train crews	$ 7,390	$ 2,060	$ 48,800	$ 58,250
Locomotive crews	15,050	2,780	46,000	63,830
Repairs	14,300	3,990	51,100	69,390
Fuel	196,100	53,700	850,000	1,099,800
Water	3,060	840	13,180	17,080
Lubricants	1,760	490	6,290	8,540
	$237,660	$63,860	$1,015,370	$1,316,890

waste for electric motive power was estimated at $1.21 per 1,000 locomotive-miles, and for the multiple-unit trains, 40 cents per 1,000 train-miles.

An average value for distribution-system maintenance costs as reported by the Paulista Company and the Central Railway of Brazil was found to be $96.50 per mile per annum.

Substation and tie-station maintenance costs were taken on the unit basis reported by the Central Railway of Brazil, since this railway's conversion equipment is similar to that projected for the Sorocabana Railway. Rectifier conversion is involved in both cases.

Summarized, the costs under electric traction are expected to amount to the figures shown in Table XI.

COMPARISON—STEAM AND ELECTRIC TRACTION

The total annual costs of items which will be affected by the change from steam to electric motive power are

Steam	$1,316,890
Electric	308,245
Difference in favor of electric	$1,008,645

In adopting electric traction for the São Paulo–Santo Antonio line, 50 steam locomotives will be made available for rental to other divisions or roads at an annual rental price of $1,500 per locomotive—a total income credit from this source of $75,000 annually.

A further credit resulting from the electrification will accrue from the release of the large number of flatcars now used for wood-fuel transport. This has been estimated at a total of $12,000 annually.

Thus, the total economy resulting from the electrification is expected to be approximately $1,100,000 per annum—a saving which when considered for a ten-year period will more than pay for the capital investment and carrying charges involved for the new system.

The Electrification

Preliminary studies for the electrification included consideration of a-c as well as d-c systems. Considerable attention was given to a 17,000-volt $16^2/_3$-cycle single-phase system proposed by continental European manufacturers, but, after comparing the economics of this system with those of the 3,000-volt d-c system, the railway company issued its call for tenders specifying the latter system. That this choice of systems is a happy one is evident from the following.

Both the Paulista Company and Central Railway of Brazil electrifications are operated at 3,000 volts direct current. The São Paulo Railway, if and when electrified, will doubtless adopt the 3,000-volt system, since it interchanges rolling stock with and handles through trains to and from both the Paulista Company and Central Railway systems. The question of suburban electrification of the Central Railway lines out of São Paulo is perennial, and unquestionably such an electrification would be operated at 3,000 volts direct current as is the Rio de Janeiro existing suburban service of the Central Railway.

In short, the 3,000-volt d-c system seems to have been adopted as the standard main-line electrification system for Brazil, although there are one or two secondary electrifications operating at 1,500 volts direct current.

Aside from questions of standardization and interchange of rolling stock, the d-c system does not involve the severe inductive interference problems commonly encountered with the a-c systems, and, as a consequence, no relocation of telephone and telegraph circuits adjacent to the railway will be necessary, except where physical interference may exist between the communications lines and the railroad's overhead contact lines.

Regenerative braking—developed to a high degree for d-c motive power—is not obtained easily with the single-phase systems, and, because of the multitude of heavy gradients and concomitant sharp curvature, electric braking is expected to be of invaluable service.

TRAIN PERFORMANCE AND LOCOMOTIVES

The schedule of freight and passenger trains set up by the railway company called for a total of 17 locomotives in active service at one time. To meet this requirement, to provide for spares, and to allow for normal stopping, a total of 20 locomotives was specified. Ten of the locomotives are to be furnished by the General Electric Company, and the Westinghouse Electric and Manufacturing Company will furnish the remaining ten.

The railroad's performance specifications stipulated that the locomotives be capable of hauling a 12-car passenger train weighing 475 tons in either direction between São Paulo at the eastern terminus of the electrification and Santo Antonio at the western end of the run, in 150 minutes. For the distance of 87 miles, this means a schedule speed of 35 miles per hour. Calculations show that the locomotive selected can meet this requirement, observing a maximum permissible speed limit of 44 miles per hour with 15 minutes leeway in time, and making two intermediate station stops.

In freight service trailing tonnages of 660 tons eastbound, and 500 tons westbound in 20-car trains in both cases are to be handled with a top speed restriction of approximately 30 miles per hour. Preponderance of tonnage moves from the interior toward the sea coast. Hence, the eastbound trains will be some 30 per cent heavier than those westbound.

These schedules, viewed from the standpoint of American railroad practice,

Table VI. Traffic Under Electric Operation, 1947

	Train-Miles	1,000 GTM
Passenger	273,000	100,500
Multiple-unit trains	181,200	26,650
Mixed	170,060	61,500
Freight	1,100,000	572,000
Totals	1,724,260	760,650

Table VII. Labor Costs Per 1,000 Train-Miles

	Operating	Repairs
Passenger	$12.90	$26.25
Multiple-unit trains	$ 6.30	$25.65
Mixed	$19.75	$19.30
Freight	$23.00	$21.80

Table VIII. Calculated Performance Figures for Sorocabana Suburban Service

Class of Service	Watt-Hours Per Ton-Mile at the Pantograph
Passenger	52.0
Multiple-unit trains	50.1
Mixed	52.0
Freight	50.0

Table IX. Energy Consumption for Various Services

Class of Service	Kilowatt-Hours at A-C Bus in Substations
Passenger	6,000,000
Multiple-unit trains	1,538,000
Mixed	3,675,000
Freight	32,850,000
Total	44,063,000

Table X. Power Charges Attributable to the Several Services Per Annum

Class of Service	Annual Power Charge
Passenger	$ 22,100
Multiple-unit trains	6,750
Mixed	13,540
Freight	121,000
Total	$163,390

Table XI. Expected Costs Under Electric Traction

Type of Service	Passenger	Suburban	Mixed	Freight	Total
Train crews	$ 3,525	$ 1,140	$ 3,370	$ 25,340	$ 33,375
Repair crews	7,175	4,650	3,290	23,960	39,075
Repair and maintenance of locomotives	6,425		4,010	25,840	36,275
Repair and maintenance of multiple-unit cars		1,285			1,285
Electric power	22,100	6,750	13,540	121,000	163,390
Lubrication	330	75	205	1,330	1,940
	$39,555	$13,900	$24,415	$197,470	$275,340
Operating costs as above					$275,340
Maintenance distribution system					20,400
Maintenance sub- and tie-station					12,505
Total					$308,245

Table XII. Dimensions and Ratings of Sorocabana Locomotives

Nomenclature.... 1−*C*+*C*−1− 238/286	6*W*374 6*GE*734*A* } 3,000 volts
Length, inside knuckles (feet–inches)	61–0
Width, over-all (feet–inches)	$9\text{–}7^{7}/_{8}$
Height over collectors, locked down (feet–inches)	$13\text{–}6^{1}/_{4}$
Wheel base, total	50–0
Wheel base, rigid	13–0
Diameter driving wheels (inches)	44
Diameter guiding wheels (inches)	33
Track gauge	1 meter
Weight, total (pounds)	286,000
Weight, on drivers	238,000
Weight, on guiding wheels	48,000
Weight, per driving axle	39,667
Number of motors and type	6-*GE*734*A*1 or 6-*W* 374
Gear ratio	75/17
Ratings on Full-Field Strength (3,000 Volts 2*S*3*P*)	
Total continuous rating (horsepower)	1,980
Total one-hour rating (horsepower)	2,280
Continuous tractive effort (pounds)	22,450
One-hour tractive effort (pounds)	27,100
Speed, continuous rating (miles per hour)	33.0
Speed, one-hour rating (miles per hour)	31.5
Adhesion, at continuous rating (per cent)	9.44
Adhesion, at one-hour rating (per cent)	11.37
Ratings on Full-Field Strength (3,000 Volts 3*S*2*P*)	
Total continuous rating (horsepower)	1,385
Total one-hour rating (horsepower)	1,800
Continuous tractive effort (pounds)	24,700
One-hour tractive effort (pounds)	35,000
Speed, continuous rating (miles per hour)	21.0
Speed, one-hour rating (miles per hour)	19.3
Adhesion, at continuous rating (per cent)	10.39
Adhesion, at one-hour rating (per cent)	14.70
Maximum permissible speed (miles per hour)	56

might be considered rather slow and leisurely, but it must be remembered that the Sorocabana is a meter-gauge railroad laid in mountainous territory with an abundance of two per cent grades and heavy curvature. If such allowances are made, it is safe to say that the performance is not only respectable but exceptionally good for narrow-gauge railroading.

In specifying motive power, the railroad stipulated that but one size and type of locomotive suitable for pooled operation in both freight- and passenger-train service be furnished. The top speed at which passenger trains may be operated—70 kilometers per hour (44 miles per hour)—and the speed-tractive-effort requirements of freight trains on the heaviest grades are such that all requirements are met very nicely with a so-called three-speed locomotive.

The tractive-effort and maximum adhesion requirements, coupled with rail-loading limitations, fixed the total weight on drivers and number of driving axles at approximately 119 tons on six axles. Maximum individual axle loading is restricted to 18,000 kilograms or 39,667 pounds, since the limiting weight of rail is 75 pounds per yard. The use of six motored axles provides for a flexible arrangement of motor combinations and, with 1,500-volt motors—insulated for 3,000-volt operation—provides for three-speed control.

Freight-train operation on the heaviest grades will be conducted with the locomotive in the 3*S*2*P* combination of traction motors—a connection that gives speeds two thirds of those obtainable in the high-speed 2*S*3*P* connection.

Operation of the locomotives in a freight-passenger pool will be facilitated by the fact that no train-heating equipment is required in passenger-train service.

Locomotive Description

A picture of one of the locomotives is shown in Figure 5.

In keeping with modern trends, the cab has been semistreamlined and, because of the absence of rivetheads, made possible by all-welded construction, will present a smooth clean appearance. No attempt was made to fair or shroud the running gear, as it was felt that accessibility for maintenance and inspection purposes would be of greater importance than a fully streamlined appearance.

Recently constructed electric locomotives have employed modified streamlined cab shapes very similar in appearance to Diesel-electric streamliners, where the operator's positions are set some distance back of a rounded nose, as, for example, in the New Haven Railroad streamlined electric locomotives placed in service in 1937. This form of construction has the advantage that protection is afforded the engineer in the event of level crossing or other collisions. In the case of the Sorocabana Railway units, however, the length of cab was definitely fixed by the railway specifications, and this requirement, in conjunction with space requirements for control and auxiliary apparatus, precluded the use of the extended-nose type of cab.

The principal dimensions and ratings of the locomotives are given in Table XII.

COMPARISON WITH STEAM LOCOMOTIVES

The locomotives are the most powerful narrow-gauge electric locomotives constructed to date. They are exceeded in continuous horsepower rating by but a few narrow-gauge steam locomotives. Some of the English-built 42-inch-gauge Beyer-Garratt locomotives working in South Africa are slightly higher in continuous horsepower rating.

The locomotives are not particularly powerful, as electric locomotives generally go, since many standard-gauge locomotives with continuous ratings in the neighborhood of 5,000 horsepower and short-time ratings of over 9,000 horsepower are currently operating in the United States. They are, however, out-

Figure 4. Typical Brazilian wood-burning locomotive being fueled

standing in that so much power has been built into a relatively small package. Particularly restricting is the space between wheels—a matter of 35$^1/_4$ inches along the axle—into which a 1,500/3,000-volt motor rating 338 horsepower continuously and 390 horsepower on the hourly basis has been placed without employing an excessively large diameter of the driving wheels. Actually the width of the motor itself exclusive of gearing is 29$^3/_4$ inches.

So far as hauling capacity is concerned, the locomotives could have been built with a six-axle $(C+C)$ running gear, but the existence of a great deal of sharp track curvature and the desirability of keeping flange wear at a minimum indicated the advisability of a guiding axle at each end. Thus an eight-axle articulated running gear with a $1-C+C-1$ wheel arrangement was included.

While it is difficult to compare electric locomotives and steam locomotives because of their dissimilar characteristics, a comparison between the largest narrow-gauge steam locomotive now in operation with the Sorocabana Railway electric locomotive is interesting. It so happens that the nominal horsepower rating of the largest Garratt articulated steam locomotive in operation on the South African 42-inch gauge lines lies between the continuous and one-hour ratings of the Sorocabana Railway unit.

Figure 7 compares the physical dimensions and weights of the two locomotives. In addition, the steam locomotive in normal service hauls a tank-car tender of water—the capacity in the locomotive tank is but 1,600 gallons.

Figure 8 compares the tractive-effort characteristics of the two locomotives.

The horsepower of the electric locomotive at 25 per cent adhesion (60,000 pounds and 30 miles per hour) is 4,800. This rating is, of course, at a considerable overload and cannot be held for any appreciable time without danger of overheating the traction motors.

The complete set of running characteristics for all connections of the traction motors and for three different values of field strength are shown in Figure 9.

The driving and guiding truck frames (Figure 6) are fabricated throughout—no castings of any kind are employed. Each driving truck employs two rolled-plate side frames, each 3$^1/_2$ inches thick, tied together with rolled-plate cross members. In appearance, the trucks are strongly suggestive of European practice. This suggestion is further emphasized by the journal boxes—these are of the Isothermos type in which no waste packing is employed. This type of box has been used to quite some extent in Europe but only to a limited extent in American railroading.

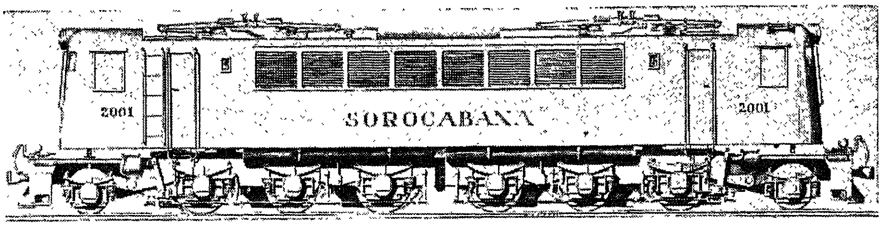


Figure 5. $1-C+C-1$ 3,000-volt electric passenger–freight locomotive

On multiple-axle locomotives, an effort is made to provide three-point suspension for the spring-borne weight of the locomotive, to allow for track irregularities, and to avoid unnecessary twisting of the truck and cab structures.

The isometric schematic diagram (Figure 10) of the main truck spring and equalizing system for one half of the running gear illustrates the three-point-suspension design employed.

The three-point-support idea is also employed in loading each truck with its share of cab weight. The two center plates in conjunction with four spring loading pads provide three points of support between the cab and each driving truck.

The articulation joint is a pin and ball-and-socket type providing full flexibility for the accommodation of trucks to track irregularities and, at the same time, mutual lateral restraint between trucks to improve tracking qualities of the locomotive as a whole. Tracking ability is further improved by providing lateral restraint in the guiding trucks. The guiding-truck restraint is provided by mounting the truck center plate on a roller-mounted carriage which climbs an inclined plane when the truck is displaced from central alignment with the main frame. The weight carried on the center plate thus acts to return the truck to central alignment.

The cab is divided into three principal compartments—two operators' compartments and a center apparatus compartment (Figure 11). The latter contains the auxiliary machinery—two 3,000/65-volt motor-generator blower sets; two 81 *CFM* exhausters and one 35 *CFM* air compressor—and the control-apparatus subcompartment. Contactors, high-speed circuit breakers, relays, accelerating resistors, and so forth, are all located in a compartment which in manufacture is completely assembled with apparatus installed before being placed in the locomotive.

The 3,000/65-volt motor-generator sets furnish power for the operation of compressor, exhausters, control, lighting; and during regenerative braking operations one of the sets acts as an exciter for traction-motor field excitation.

Ventilating air for traction motors, accelerating resistors, motor-generator sets, and exhauster motors is furnished by two blowers driven directly by the 3,000-volt motors of the two motor-generator sets.

Control System

The control system is of the single-unit nonautomatic type with 33 accelerating steps and three running-motor combinations. Each combination of motors

6 series
3 series 2 parallel
2 series 3 parallel

provides one step of full-field and two steps of reduced-field strength, thus providing a total of nine running steps as shown in the speed-tractive-effort characteristics.

Regenerative braking is provided for all three motor combinations. While a multitude of short two per cent grades on the line between São Paulo and Santo Antonio makes regenerative braking desirable, the braking feature will be particu-

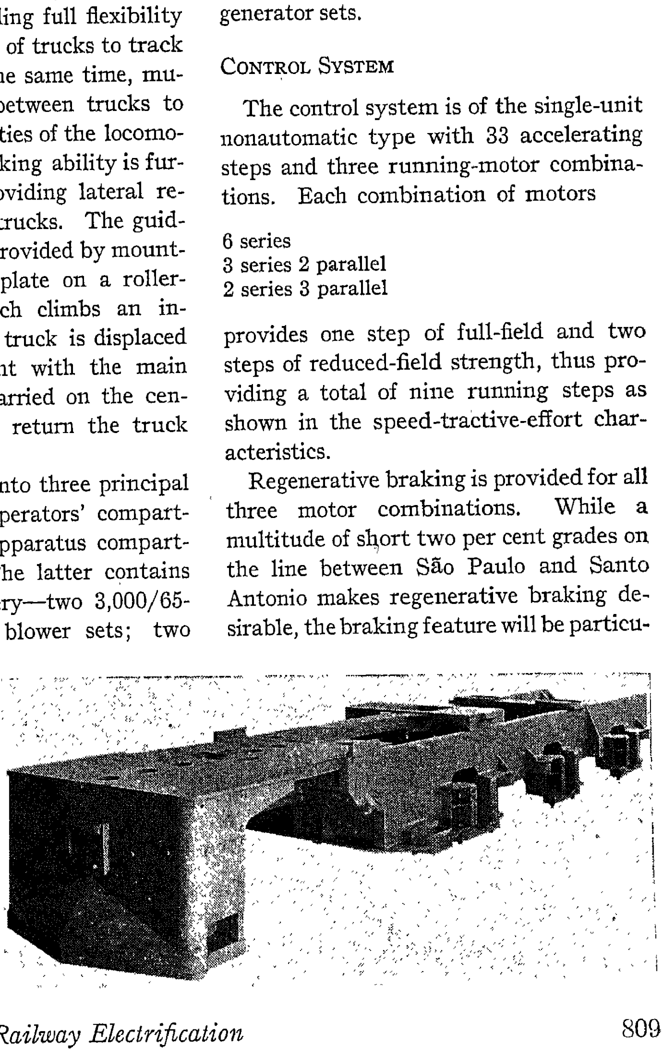


Figure 6. All-welded fabricated main-truck-frame assembly for locomotive, classification $1-C+C-1$, 130 metric tons, 3,000 volts direct current meter-gauge

cularly advantageous in the operation of the 25-mile grade—which averages very nearly two per cent—on the Mayrink–Santos line which will be electrified at some future date.

Despite the fact that ten locomotives are to be furnished by the General Electric Company and ten by the Westinghouse Company, both makes will be built to the same specifications. The mechanical portions will be identical—all 20 will be built by the General Electric Company.

The traction motors as built by the two companies will be exact duplicates as far as their electrical characteristics are concerned and will be interchangeable physically.

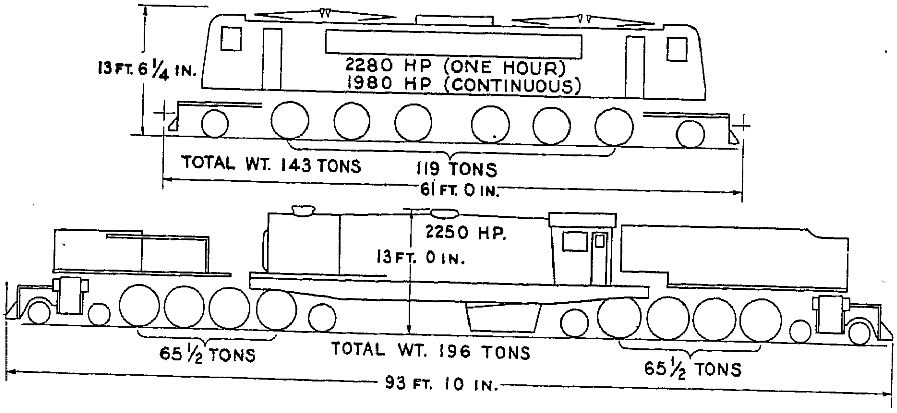
Much of the auxiliary and control equipment will be interchangeable between the locomotives manufactured by the two companies. Pantographs are identical. Motor–generator sets are interchangeable physically and have the same electrical characteristics. Traction-motor blowers are identical. High-speed circuit breakers will be identical, and all 20 will be manufactured by one company.

Multiple-Unit Cars

To handle existing suburban traffic and in anticipation of future commuter traffic expected with the extension of residential sections west of the city of São Paulo, four three-car multiple-unit trains have been specified as a part of the electrification.

These are to be basic units of three cars—a four-motor motorcar with a nonmotored operating trailer at each end of the motorcar. While the basic train unit will consist of three cars as described, an operating position at one end of the motorcar is to be provided, thus permitting of single-end operation of the motorcar alone, or double-end operation of a motor–trailer combination, should such operation be deemed expedient.

Figure 7. Dimensional comparison of Sorocabana 3,000-volt meter-gauge electric locomotive (top) with South African Railway Garratt articulated 42-inch-gauge steam locomotive (bottom)



These multiple-unit cars will be the first 3,000-volt meter-gauge multiple-unit cars to be placed in service. As a matter of fact, there is in operation but one 3,000-volt meter-gauge electrification—the Transandine line over the Andes between Chile and the Argentine. This line utilizes no multiple-unit car equipment—only rack-and-adhesion locomotives.

Schedules to be run by the multiple-unit trains are based on an accelerating rate of 1.5 kilometers per hour per second, which is equivalent to approximately 1.0 miles per hour per second. Braking rates of 1.5 mile per hour per second will be employed. Schedules are to be maintained with a top permissible speed of approximately 40 miles per hour.

The traction motors will be self-ventilated by fans mounted on their own armatures, taking clean air from a duct system the intakes of which are located in the car roofs. This ventilating system is quite similar to that used on the 3,000-volt Lackawanna Railroad multiple-unit cars. It has been found advisable on 3,000-volt self-ventilated motors in multiple-unit car service to take the air at a level high above the track, and to preclude the induction of brake-shoe dust and other foreign matter that would cause insulation breakdown and flashovers.

The 3,000-volt motor on the motor-generator set also takes its air from the duct system.

The control is of the *PCM* type with all 3,000-volt control located in boxes beneath the car flooring.

Control and lighting energy at 65 volts for all three cars in the basic train is to be furnished by the 3,000/65-volt ten-kilowatt motor-generator set and a 32-cell 60 *A-H* lead-acid type of storage battery.

Two running speeds 4*S* and 2*S*2*P* with no field shunting are provided. The acceleration will be nonautomatic—that is, the driver will have complete control during acceleration.

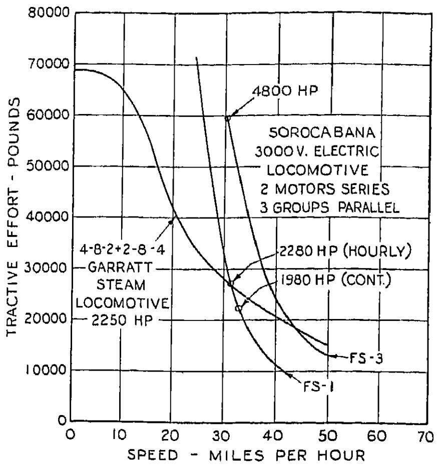


Figure 8. Comparison of tractive-effort characteristics of Sorocabana 3,000-volt meter-gauge electric locomotive and 42-inch-gauge steam locomotive

Air brakes are to be furnished, and a 65-volt 35 *CFM* compressor operating off the motor-generator set will furnish air.

Traction motors are rated at 185 horsepower (one hour) and 148 horsepower (continuous).

The electrical equipment for the multiple-unit cars will be built by the General Electric Company, while the cars will be built and equipped at the Worcester, Mass., plant of the Pullman-Standard Car Manufacturing Company.

Overhead Distribution System

Before the overhead system is described, it should be noted first that two major factors greatly influenced design. These are weather and cost of labor. These two factors should be kept in mind for they will serve to explain some of the deviations made from North American practice.

The weather in Brazil is mild. Freezing temperatures do not occur in the territory involved in the electrification, and the maximum temperature change amounts to only 80 degrees Fahrenheit. The wind pressure on structures and cables barely exceeds eight pounds per square foot.

Common labor in Brazil receives the equivalent of three cents to six cents per hour in United States currency and skilled labor about twice this amount. These two factors, weather and labor cost, have greatly changed the ratio of material cost to labor cost. Structures have been made lighter and of different materials from those which are normally used in the United States, and considerable field labor will be substituted for higher-priced shop labor.

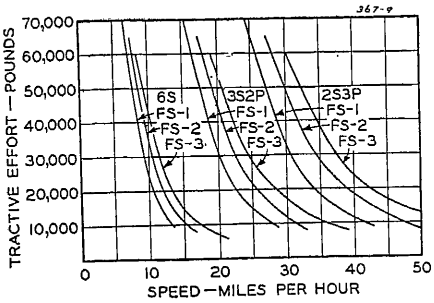


Figure 9. Speed tractive-effort characteristics of Sorocabana 3,000-volt meter-gauge electric locomotive

The distribution system extends approximately 87 miles from the city of São Paulo. The railroad is double-tracked throughout this distance with five main yards at São Paulo, Barra Funda, Mayrink, Sorocabana and Santo Antonio. Two types of catenary, a main-line type and a yard type, were designed for the system. With the exception of a ground wire and pilot wires for electrical machine control, no wires and cables, other than catenary, are supported on the structures. Steel structures will be erected over the first ten kilometers of electrification extending out from São Paulo, and also over most of the yard tracks in other locations. Concrete poles supporting cross spans will be used for the main-line two-track suspension. The return system uses the electric arc-weld bond on each track joint.

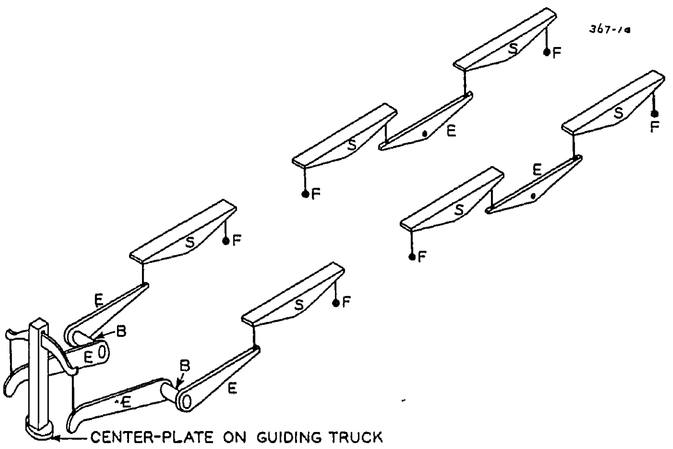


Figure 10. Isometric of three-point suspension

Clearance

Although the railway operates over meter-gauge track, the clearance diagram does not deviate materially from that of a standard-gauge railroad. The distance between track centers is 13 feet, the distance from the center of track to the face of structures is 8 feet, and the head clearance is 15 feet, 9 inches. The trolley-wire height above rail is 18 feet (normal), and the maximum deviation of trolley-wire alignment from the center line of track is 9 inches, either side of center.

Structures

The design and spacing of structures were determined by a study of track alignment, loadings, and costs. In some locations, however, structure designs are altered from standard because of limited clearances caused by walls and buildings. A structure spacing of 60 meters (197 feet) on tangent limited the dead load to 500 pounds at each suspension point. This dead load includes approximately 750,000 circular mils of copper in the messenger and trolley wires over each track, as well as catenary hardware and insulation. There is no ice load. This 500-pound dead load is not a heavy loading when compared to some of the catenary spans on the Pennsylvania, Lackawanna, New Haven, or Reading electrified systems. Using this dead load of 500 pounds per suspension point and the wind load, a study of several types of structures was made. These types included:

1. The two-track portal or bridge type fabricated with steel.
2. The steel-pole cross span.
3. The steel-pole two-track bracket.
4. The concrete-pole cross span.

In the United States there would be very little doubt as to the type of structure erected. If the engineer favored the bridge type of structure, he could no doubt prove the bridge structure to be the most economical. If on the other hand, he favored the cross-span type of structure, he could easily prove the cross span to be the most economical. In the United States there is very little difference in the costs of the two types of structures for comparatively light loadings. However, in Brazil the low cost of labor makes a material difference. Steel must be fabricated 6,000 miles from the field. The dimensions of the structure must originate in Brazil. The design is made in the United States of America and then sent to Brazil to be checked. After the structures are fabricated in the United States of America, they are shipped by rail to the seaboard, then reshipped by boat to Santos, and again shipped by rail to the field. Compare the cost of this procedure for steel structures with the cost of the manufacture of reinforced concrete poles where all ingredients are easily available and skilled concrete workers comparatively plentiful. For Brazil, with a favorable climate, has developed the manufacture and use of concrete beyond that of many countries, just as California and Florida have developed the use of stucco in the United States of America.

On tangent track, the side load on poles supporting cross spans is only 1,800 pounds. The guy load is approximately twice this figure (3,600 pounds). On the most extreme curves, where pullover poles are placed between supporting structures, the side load on the poles, due to double track catenary, does not exceed 3,500 pounds. The guy load would be approximately 7,000 pounds. With these light loads, concrete poles, properly guyed, are more economical than fabricated steel structures, or bridge structures built with simple shapes.

Although all structures must be guyed to prevent excessive bending moment, the guy cost is comparatively low. The total cost of a guyed concrete structure, including the digging of holes, concrete guy slugs, span wire, and hardware, is considerably less than the cost of an unguyed steel structure of the portal type, or the guyed cross-span structure using two steel *CB* columns. The Paulista Railway has constructed many miles of catenary similar to the Sorocabana design with guyed concrete poles.

Concrete Poles

The standard concrete pole for tangent and curve two-track construction is rectangular in shape. The pole tapers from the top to the ground line, and a reverse taper is carried from the ground line to the butt. In other words, the pole has a swelled section at the ground line. Three heights of concrete poles are being manufactured with heights of 33, 36, and 40 feet. At the top all poles are 8 by 10 inches, at the ground line 14 by 20 inches, and at the butt 14 by 14 inches. These poles are placed with the long dimension parallel to the track. The depth of the

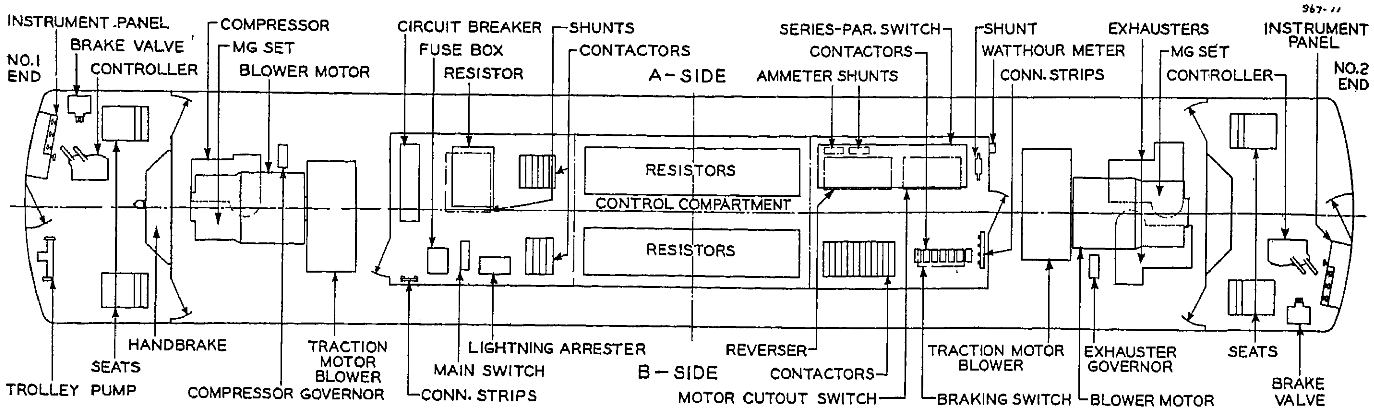


Figure 11. Cab layout of Sorocabana 3,000-volt meter-gauge electric locomotive

average hole is six feet in undisturbed earth. All poles will be manufactured in a pole yard at a convenient shipping point along the railroad.

The design of the supporting pole was made by Cobrazil, the construction organization at São Paulo. The Brazilian Association of Portland Cement was also consulted with respect to the design. These poles will be cast in wood forms. As is usually the case in the manufacture of concrete poles in Brazil, these poles will have a hollow center to reduce weight.

The poles will be reinforced with $^3/_8$-inch rods at the corners and with $^5/_{16}$-inch steel wrapping. Specifications call for a tensile strength of 28,000 pounds per square inch for reinforcing steel and a compressive strength of 1,400 pounds per square inch for the concrete. The concrete is not expected to take any tension. Yield at the end of 28 days is 3,500 pounds per square inch. All concrete will be vibrated during construction, and the hollow core will be filled with water to aid in curing. The ratio of the moduli of steel to concrete for design purposes is 15. A concrete mix of 4:2:$1^1/_4$ is being used.

All span and guy wires and the pilot-wire crossarms will attach to pole collars, and in view of this no provision is made in the pole itself for these attachments. However, a hole will be cut through the wall of the pole below the ground line for ground connection to the vertical ground tap carried in the hollow center of the pole.

Guys

The comparatively light side loads on the concrete two-track structures aided in solving the guy problem. The reinforced concrete pole itself is not considered a good ground, and therefore, principally for protection against electrical shock, all concrete pole guys will be insulated. Heavy guy cables require heavy and expensive insulators and insulator fittings. Insulators and fittings for light guys are comparatively cheap. On the standard tangent two-track cross span, each concrete pole is guyed with a $^7/_{16}$-inch Siemens-Martin seven-wire galvanized steel cable. Since the guy load does not exceed 3,600 pounds, this cable with a 9,350-pound ultimate is quite satisfactory. Corrosion is not a factor in the selection of cable sizes. The maximum guy load on two-track curves is 7,000 pounds; therefore two $^7/_{16}$-inch Siemens-Martin cables are satisfactory. On the standard two-track structures these guys are attached at separate locations, one guy opposite the supporting span and the other guy opposite the trolley-wire pullover span. The side load on intermediate pullover poles occurs at a single location; therefore the guy load is concentrated at one point. On these poles double $^7/_{16}$-inch guys are attached to a triangular equalizer plate at the pole band and to the same guy anchor in the ground. This arrangement can be seen in Figure 13. These light guys have been responsible for the elimination of many turnbuckles necessary with heavier cables.

Steel Structures

In general, the field forces select the type of supporting structures. Having satisfactorily located the structures, and having determined building and guy clearances, also footing restrictions, the field men decide whether a structure will be steel or concrete, bridge, cross span, or bracket. Wiring drawings are prepared in São Paulo showing the location of all structures and overhead, and in addition separate sketches are made of all structures. This information is used in the United States as the basis of structure design.

The two-track tangent steel structure consists of two ten-inch 33-pound *CB* columns and an eight-inch 31-pound *CB* beam. Including knee braces, the total weight of this structure is approximately 3,500 pounds.

The two-track curve structure for the heaviest loads consists of two ten-inch 49-pound columns and a ten-inch 41-pound beam. With knee braces this structure averages approximately 4,500 pounds.

The three-track structure will be built with *CB* columns and a *CB* beam, but the trusses for spans crossing four tracks or more will be latticed. The depth of trusses for spans varying from 45 to 60 feet is 56 inches; from 60 feet to 90 feet 54 inches; and for the one long span of 112 feet, the depth will be 72 inches. The general design of truss embodies four corner angles and $2^1/_2$ by $2^1/_2$ by $^5/_{16}$-inches angle iron lacing, flat side of lacing to flat side of corner angle.

When installing catenary over large yards, clearance between tracks is usually insufficient for the most economical columns. Many of the long trusses are supported with thin columns consisting of two *CB* legs, latticed. In some instances these columns are similar to the well-known *A* frame used for supporting signal towers. Riveted assemblies are almost entirely eliminated because of the additional space they would require in shipping. All steel will be bolted with three-fourth-inch bolts in the field. Angle iron for latticing will be connected to the corner angles with two bolts rather than one bolt.

A maximum fiber stress of 13,300 pounds per square inch was used in designing this steel. The point of zero moment for the column was located one third of the length of the unsupported column above ground. In addition to the dead loads, side loads, and wind loads, each structure was designed to withstand a single 2,000-pound longitudinal load at any catenary support. This longitudinal load was added to handle messenger breakage, the slipping point of the messenger clamp occurring somewhat below 2,000 pounds. In calculating steel for the 2,000-pound longitudinal load, it was felt that, since messenger breaks are extremely rare, the maximum fiber stress for these loads could be increased to approximately the yield figure.

Footings

The average depth of hole for the two-track concrete poles, tangent and curve, is six feet. Since all poles are guyed laterally, and since the longitudinal load caused by messenger break will not exceed 1,000 pounds, no additional concrete for side bearing will be cast around the base of these poles. However, precast bearing blocks will be placed in the bottom of all holes. For tangent poles, the bearing block will be 24 inches in diameter, and for curve poles it will be 30 inches in diameter.

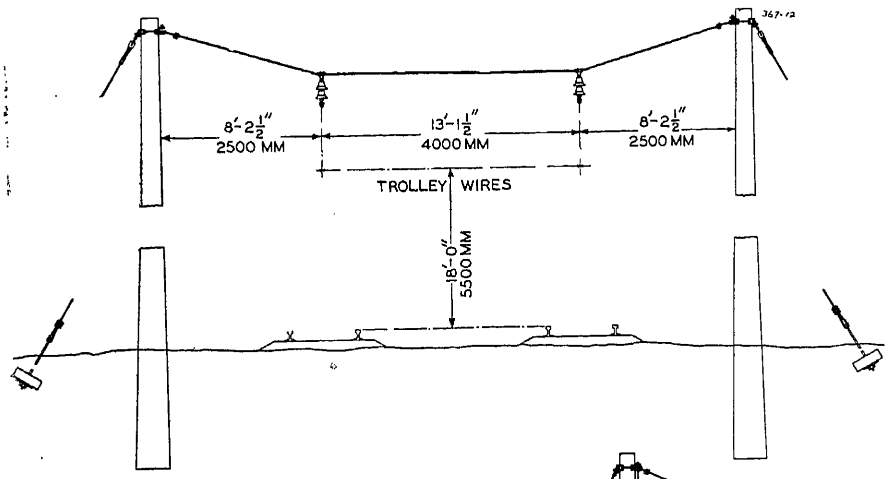


Figure 12 Tangent structure

The ten-inch *CB* columns for both tangent and curve portal structures, will be set on concrete pads similar to those used for concrete poles and, in addition, will be embedded in concrete to a point one foot above ground level. Since these structures are not guyed, the foundations must be constructed for side bearing. As a consequence, the tangent-structure foundations will be at least 18 inches in diameter, and the curve-structure foundations will be approximately 30 inches in diameter. The depth of foundations for two-track steel structures, including the bottom pad, is 7 feet below ground line.

Special footings are necessary for the larger structures, the cross sections and depths corresponding to the loads. All columns will be embedded in the concrete foundations, no anchor bolt structures being used.

Cross Spans

All cross spans or supporting spans are attached to pole clamps on concrete poles.

On the tangent structure, as illustrated in Figure 12, the pole clamp is also the attachment for the ground guy. The cross span is $^7/_{16}$-inch Siemens-Martin seven-wire steel cable, and, where it attaches to pole clamps or guy rods, it is protected by either a thimble or large radius curved section. Two-bolt clamps with a wave seat are used for light loads and three-bolt clamps with a similar seat for heavy loads. The two-bolt clamp corresponds to two Crosby clips with respect to slippage, and the three-bolt clamp has almost the strength of four Crosby clips. All guys are insulated near the top of the pole. Insulator strings are attached to the span wire with inverted suspension clamps.

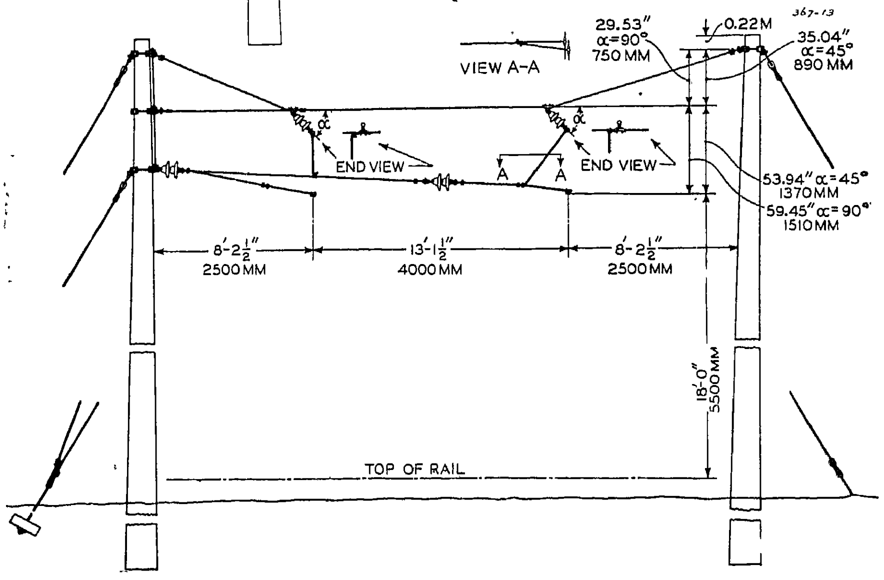


Figure 13. Two-track curve structure

Attachments to Steel Structures

On steel structures built with a single *CB* beam spanning one, two, or three tracks, the insulator string is attached with a beam clamp. On latticed structures an angle iron fitting is attached to the lower chord of the truss, and the insulator string is attached to the angle iron. Span attachments to the *CB* columns are made with structural attachments that provide vertical adjustment. Guy attachments to *CB* columns are made with a half section of *CB* column bolted to the structure column.

Catenary Spans

The main-line catenary span shown in Figure 15 is 60 meters (197 feet) in length The main messenger is a 300,000-circular-mil hard-drawn-copper cable and supports two 4/0 hard-drawn-copper contact wires. Each contact wire is supported by alternate hangers. This system, with the exception of dimensions and capacity, is similar to that used on the Chicago, Milwaukee, St. Paul, and Pacific Railroad as well as on the Paulista Railway in Brazil.

The normal messenger tension is 4,000 pounds and the normal trolley-wire tension is 2,000 pounds. With the low temperature range in the vicinity of the electrification, the messenger tensions will vary from 3,500 pounds at 100 degrees Fahrenheit to 4,800 pounds at 30 degrees Fahrenheit. The normal sag is 33 inches. The secondary catenary span, Figure 16, used in yards includes a $^7/_{16}$-inch Siemens-Martin messenger and a single 4/0 trolley wire. The normal sag of this span is also 33 inches.

A flexible-loop catenary hanger will support the trolley wire. On main-line spans this hanger will ride on a cast bronze shim or protecting sleeve that surrounds the copper messenger cable.

The sleeve is necessary to protect the messenger cable from mechanical wear and arcing.

Curve Construction

Chord construction was selected for this meter-gauge electrification. On severe curves a pullover pole is placed between supporting structures in order to provide a means for aligning the catenary systems with the center line of track. The supporting structure designed for curve construction, as shown in Figure 14, includes the same cross-span supporting cable as used for tangent spans. The side load produced by the messenger cables on curves is carried to the outside concrete pole by a short pullover cable which is really a continuation of the center portion of the supporting span. The contact wires are aligned with a separate pullover cable that attaches to a pole collar on the concrete pole. The catenary systems over the two tracks are insulated from each other as can be seen in the illustration. The pullover poles located between supporting structures will have the messenger pullover and trolley-wire pullover combined and attached at one point on the pole.

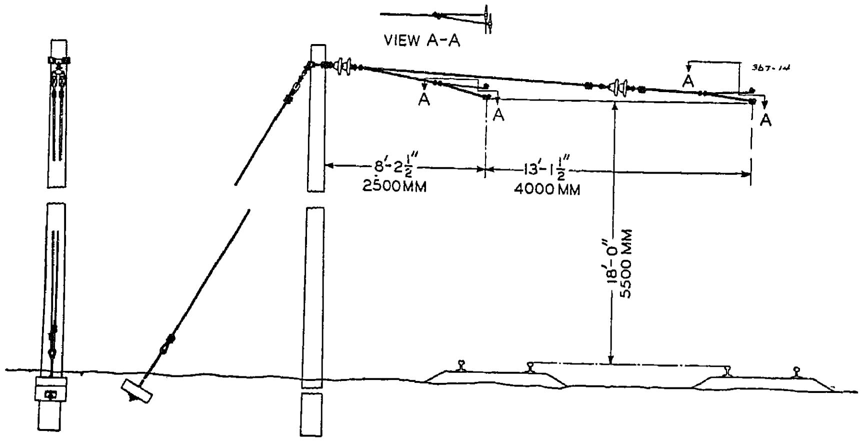


Figure 14. Mid-span pull-over

"Steadies"

The two-track "steady" will be installed at 1,200-foot intervals. In this design, a steady rod attached to the trolley wire over the left-hand track prevents sway of the trolley wires, because of its anchorage to a span running between the two messenger cables. The steady arm is attached to only one of the two-contact wires in the same span.

Anchors

Anchorages will be installed at 4,500-foot intervals. At each anchor point the messenger cables are anchored in both directions.

Ground Wire

A three-eighths-inch steel ground wire is carried the length of the electrification. This ground wire is connected to all metallic collars, spans, and so forth, attached to the concrete poles. Each pole collar is designed for the attachment of the vertical ground tap, as well as for the attachment of the aerial ground wire. This pole collar is used universally for span connections and ground connections. The ground tap to rail is carried down the center of the pole and out of the pole at a point just below ground line. At this point a tap is carried to the rail and also to a ground rod.

Sectionalizing

Most of the sectionalizing points occur near railroad stations. They are so arranged that the station operator can manipulate the switches in order to isolate a section of line. A new-type double-trolley-wire-section insulator has been used throughout the main line and, because of its extremely light weight, will be satisfactory for the maximum speeds. This section insulator is constructed with phenolic tubes and weighs only 29 pounds.

Bonds

All track joints are bonded with a 4/0 seven-inch U-shaped arc-weld bond. Since this system has 3,000 volts on the contact line, portable gasoline-driven welding equipment will furnish energy to the welders. In addition to the regular track-joint welding, cross bonds will be installed at 1,000-foot intervals.

Distribution system material is being supplied by the Ohio Brass Company.

Substations

Power will be supplied to the 3,000-volt overhead trolley by means of three converting substations. These substations are located approximately ten miles from each terminal and at the mid-point of the electrified section (Figure 2). Each of the substations will be supplied individually from the interconnected circuits of the São Paulo Tramway, Light and Power Company over double-circuit lines. Two of the substations will receive power at a high voltage of 88,000 and the third substation at 44,000 volts. However, it is anticipated that at the latter station the high voltage will also be 88,000 in the near future, and consequently it is designed for future operation at this voltage. Two of the substation locations will be served by existing lines, and the third location requires the construction of only 15 miles of transmission line. This will be constructed by the power company.

Each of the three substations (Figures 17 and 18) will be equipped with three 2,000-kw rectifier units, one of which serves as a spare. Naturally the substation located at the mid-point carries more load than either of the other two. However, it is anticipated that the rapid

Figure 15. Main-line contact span

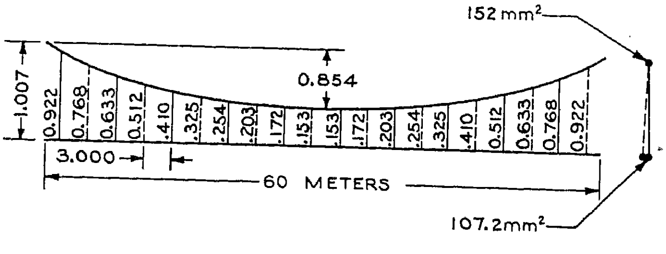
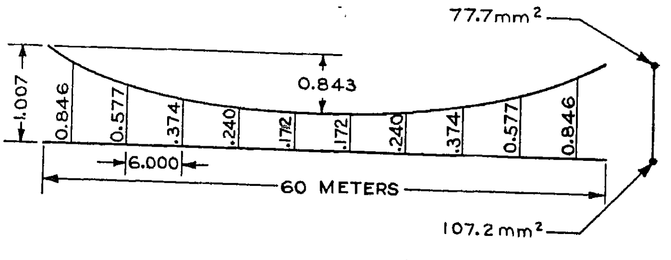


Figure 16. Side-track contact span



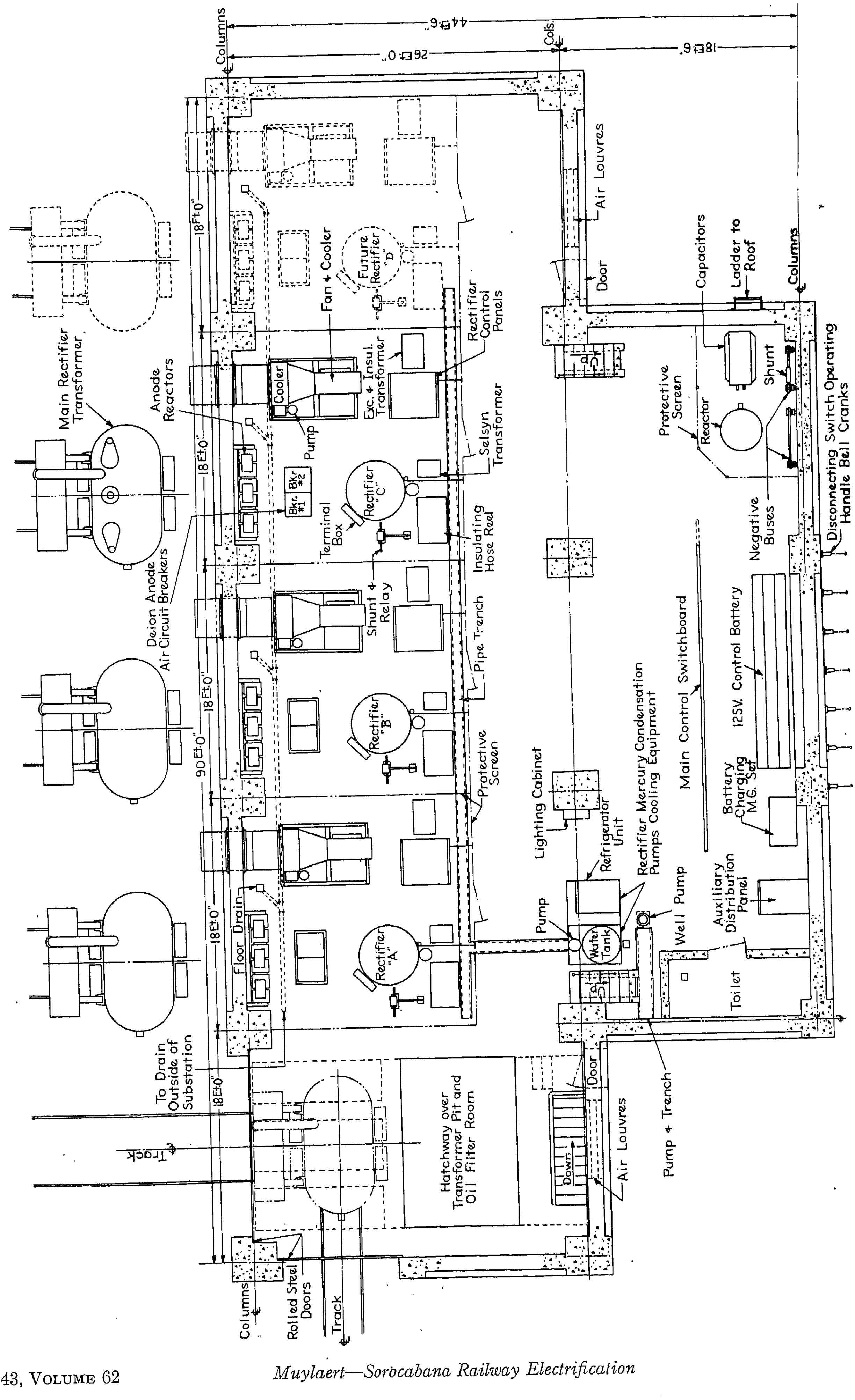


Figure 17. Plan view of 6,000-kw 3,000-volt mercury-arc-rectifier d-c substations for Sorocabana Railway

and improved suburban service with electric operation will greatly stimulate the growth of the suburban communities along the line of the railway, thereby increasing the load on this substation. Then, too, with the continually increasing traffic, it is quite certain that the electrification of the main line will be extended from Santo Antonio to Botucatú, approximately 90 miles beyond the western terminal of the section now being electrified.

The high-voltage switchgear will be located outdoors and will be supported by a firmly anchored galvanized steel structure. Adjacent to this structure will be the building for housing the rectifiers and their associated apparatus together with the d-c switchgear. For ultimate economy, each substation is designed to receive a fourth unit at some future date.

Rectifiers

Rectifiers were selected rather than motor–generator sets for the conversion of the 60-cycle a-c power to 3,000 volts direct current, because of high efficiency, ability to carry heavy overloads without injury, economy of substation design, reduced maintenance, and so forth. The rectifiers (a factory view of a rectifier is shown in Figure 19) will be of the single-tank multianode type, equipped with the usual complement of main and auxiliary anodes, vacuum pumps, and gauges. The main anodes are surrounded by grids which separate and isolate them electrically from the vacuum chamber. These grids permit high-speed regulation of voltage and accurate division of load between the rectifiers in the same substation. The quick operation of the regulating equipment will hold the rectifier voltage practically constant in the range from no load to full load. On overloads, the rectifiers will operate on their normal characteristic having approximately six per cent regulation; that is, between full load and three times full load, the voltage will drop from 3,000 volts to 2,640 volts.

The rectifier will be cooled by means of a recirculating cooling equipment consisting of a pump, fan, radiator, and water-storage tank. The water-circulating pump will operate continuously. The motor-driven fan will be operated only as required to reduce the temperature of the cooling water. Heaters in the water-storage compartment will be energized as required during light-load or no-load periods to maintain a definite minimum temperature for best operation of the rectifier. The fan and heaters will be controlled by thermostats which operate in response to the control-point temperatures of the rectifier. The mercury-condensation vacuum pump of the rectifier will be cooled by a similar type of equipment. The water in the recirculating equipments will be treated to prevent corrosion.

To reduce the ripple in the d-c voltage, a d-c smoothing reactor will be provided for each substation. In addition, a resonant shunt will be provided which consists of three resonant coils and reactors tuned for the 6th, 12th, and 18th harmonics in the rectified current. The amplitude of these harmonic frequencies will be reduced to one tenth of the value that would prevail without the use of this filtering equipment.

Each rectifier unit will be supplied with power from a three-phase oil-immersed self-cooled outdoor transformer which transforms from the 88,000 or 44,000 high voltage to that necessary for the 3,000 rectified d-c voltage. The overall efficiency of the rectifier unit from the high-voltage terminals of the main transformer to the d-c terminals of the rectifier will be 97 per cent or more between

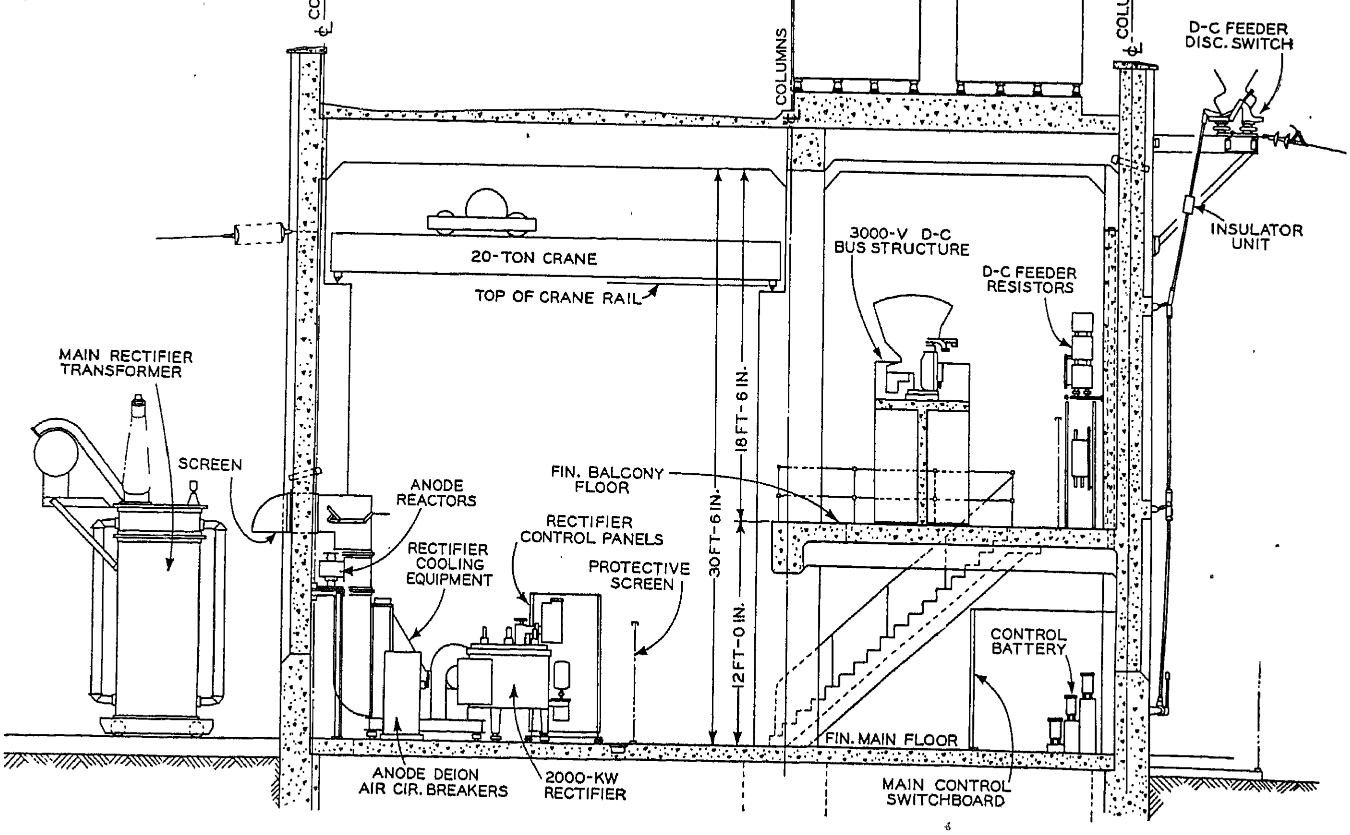


Figure 18. Vertical section of 6,000-kw 3,000-volt mercury-arc-rectifier d-c substation for Sorocabana Railway

one-half load and 150 per cent load. The units will be capable of meeting the following overload ratings: 150 per cent load for two hours, 240 per cent load for three minutes, and 300 per cent load for one minute.

Regenerative-Power-Absorption Resistors

While the application of regenerative braking to the locomotives is readily justifiable, because of the serrated profile with its numerous steep grades, several factors contribute toward making it undesirable to arrange the substations to return this regenerated power back into the supply system. Traffic on the railway is moved by a reasonably large number of relatively small trains. This fact, together with the nature of the profile, will usually result in the power regenerated by a train on a descending grade being consumed entirely by a train ascending an adjacent grade. Detailed calculations based on a typical daily train sheet show that the surplus of regenerated power averages only about two per cent. Not only will the surplus of regenerated power be small, but also the railway would receive no credit if it were put back into the power-supply system. Consequently, the expenditure for inverter units or the complicated control apparatus necessary to change from rectifier to inverter operation was not justified.

In view of the preceding, a large resistor is provided at each substation for absorbing the excess regenerated power from the trolley system. This resistor is capable of absorbing 1,800 kw of energy and will be mounted on the roof of the substation building. It is divided into nine equal steps which are connected to or disconnected from the 3,000-volt system by automatic-control functioning upon indication of a high-voltage relay and a load-indicating relay connected to the substation bus. An excess of regenerated power will cause the substation bus voltage to rise above normal. This rise in voltage actuates the control which automatically cuts in the load resistor step by step as required to produce a relatively stable voltage condition at the bus. When the traction load again predominates, the resistor is automatically cut out of the circuit.

Switchgear

Each of the substations will be fed by one of two 60-cycle three-phase lines which are tapped off the power company's distribution system. At the point where these transmission feeders connect to the power system, the power company will install high-voltage oil circuit breakers with metering and protective equipment. Since this point of connection is practically adjacent to the substation at two of the locations, motor-operated disconnecting switches with deion fuses will be used in the transmission feeder lines. At the third location, the transmission feeders between the substation and the power system are approximately 15 miles long, and here high-voltage oil circuit breakers with overload protective relays will be provided. At all stations, motor-operated disconnecting switches and fuses will be used between the high-voltage bus and the primary side of the power transformers. For the protection of the secondary system against short circuits, circuit breakers will be provided in the rectifier anode leads. All d-c switching will be accomplished by high-speed breakers. The use of high-speed cathode breakers provides rapid disconnection of the rectifier from the substation bus in the event of an arc-back. The d-c feeder breakers are essentially a duplicate of the rectifier cathode breakers, and the control circuit is of the standard short-circuit-detecting automatic-reclosing type.

Any of the three rectifier units in a given station may be operated as the base load unit. The other two units will be started and stopped in sequence as load conditions demand. A selector switch and over- and underload relays form the essential parts of the automatic equipment which controls the operation of the three rectifiers. The position of the sequence switch determines which of the three rectifiers is to operate continuously and also the sequence in which the other two units will be started and stopped. The scheme of control and layout of wiring is so arranged that, with the addition of a fourth rectifier unit, a four-unit sequence will prevail without the addition of any other control apparatus except that associated directly with the new rectifier.

Control Energy

Control energy for the auxiliary devices of the mercury-arc rectifier, such as equipment for cooling, vacuum pumping, and excitation, will be supplied from a 100-kw three-phase 60-cycle operating transformer. The secondary voltage of this transformer is 220 volts.

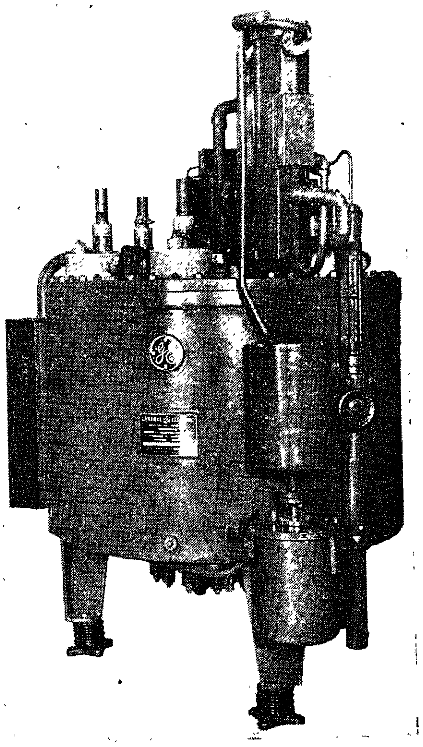


Figure 19. Multianode mercury-arc rectifie for railway service, rated 2,000 kw, 3,000 volts, type *RHW*

Control energy for the automatic devices for closing and opening the high-voltage motor-operated disconnecting switches, the anode circuit breakers, and the contactors for the regenerative-power-absorbing resistor will be taken from a 125-volt battery. This battery will be charged by means of a motor–generator set which receives its energy from the previously mentioned operating transformer. The battery-charging motor–generator set normally will supply the control energy and deliver a trickle charge to the battery. The battery will supply short-time demands beyond the capacity of the motor–generator set.

Metering Equipment

Metering and potential transformers will be installed as a part of the high-voltage equipment in each station. Recording demand meter and wattmeter will indicate the load on each individual substation. It is contemplated that remote metering equipment providing for totalizing the demand of all three substations at one point will be added at a future date. This will require the addition of contact-making watt-hour meters to transmit impulses over a direct-wire line to the dispatcher's office. In this office, an impulse totalizer will record the impulses and indicate the maximum power demand in the desired time interval.

Operator's Quarters

While these stations are provided with full automatic control, a watchman-inspector will be assigned to each station.

The inspector's duties, in addition to policing the grounds, consist merely of cleaning and inspecting the equipment. At any time that anything occurs to lock out the station, an alarm rings in the inspector's quarters so that he is available immediately to investigate the trouble and report to central headquarters. Since this contract calls for the electrification completely installed and ready to run, all installation materials, installation drawings, building design, and construction, and so forth are to be included. These include the design and construction of living quarters for the operator at each substation

TIE STATIONS

In order to reduce the conductivity required in the trolley circuit by taking advantage of the diversity of load on the two tracks, tie stations will be installed approximately midway between the substations. At these points the two trolley circuits will be tied together through high-speed circuit breakers. Each tie station will have a 3,000-volt positive bus, and four high-speed breakers will connect the trolley circuits to this bus. The high-speed breakers in the tie station are tripped by short-circuit currents flowing from the bus into the trolley system. A flow of power in the reverse direction will not cause the breaker to trip. In this manner, only the breakers feeding the faulty circuit will open and isolate the fault. A pilot-wire system is used connecting the control of the breaker in the tie station with that in the rectifier substation. The control scheme is so arranged that opening of a breaker at either end of the trolley will cause the breaker at the opposite end to open, regardless of whether or not it may be tripped by the fault current. This insures removal of energy from the faulty circuit. A few seconds after the breakers are opened, the automatic equipment in the rectifier substation will function to check the resistance of the load circuit, and, if the fault has been cleared, this breaker will reclose automatically. As soon as the rectifier-substation-feeder breaker closes, supplying energy to the trolley system, a voltage relay in the tie station will operate to deliver the closing impulse to the breaker in that station. The pilot-wire control scheme is so arranged that, if the circuits become open, both the substation and tie-station breakers will be tripped and will remain open until attendants at both ends of the trolley section transfer the control of the feeder breakers over to local operation independent of the pilot-wire circuits.

DIVISION OF APPARATUS

In order to meet the stipulation that apparatus in all substations must be duplicate, the division of apparatus to be supplied by the two manufacturers was made on the basis of partial equipment for each substation. The rectifiers with their associated apparatus and the main power transformers will be supplied by the General Electric Company and the a-c switchgear and d-c switchgear for both the substations and tie stations will be supplied by the Westinghouse company.

Inasmuch as this is an entirely new installation, there is no necessity for following existing practices or standards. Consequently, it was possible to provide a fully automatic scheme of control based on standard practices and proved circuits with minimum apparatus and maximum simplicity of circuits.

The contract for the manufacture of apparatus and equipment and the complete installation of the electrification was placed with the Electrical Export Corporation—a combination consisting of the International General Electric Company; the Westinghouse International Company and a Brazilian constructing company—Cobrazil (Companhia de Mineracao e Metalurgia Brasil). Substation apparatus, locomotives, and electrical equipment for the multiple-unit cars are being furnished by the General Electric and the Westinghouse Electric and Manufacturing Companies.

Distribution system material is being supplied by the Ohio Brass Company and by the American Bridge Company.

The Pullman-Standard Car Manufacturing Company is building the suburban multiple-unit rolling stock.

The Effect of Corona on Coupling Factors Between Ground Wires and Phase Conductors

G. D. McCANN
ASSOCIATE AIEE

Synopsis: Measurements of coupling factors between ground wires and phase conductors, as a function of ground-wire voltage, have been made for a sufficient number of cases to determine them for any of the standard transmission-line configurations. It was found that the effect of corona can be expressed in terms of an effective corona ground-wire radius to be used in the conventional coupling-factor equations. Curves are also presented for the quick determination of coupling factors for all standard line configurations. A photographic study of the visual characteristics of surge corona was made for a comparison with the effective radius and with 60-cycle corona.

Actual measurements were made for potentials up to about 2,000 kv, but the data were extrapolated to 6,000 kv with what is considered to be reasonably good accuracy. Coupling factors for positive polarity were found to be considerably higher than those for negative polarity and both higher than values previously calculated from purely theoretical considerations. For a potential of 1,000 kv the actual coupling is 1.2 to 1.5 and 1.4 to 1.8 times as great; for negative and positive polarity respectively, as the values obtained by assuming no corona. At 2,000 kv the corresponding figures are 1.4 to 2 and 1.6 to 2.7 respectively, and at 4,000 kv 1.7 to 2.9 and 2 to 4.

Ground-wire size was found to be unimportant above about 200 kv and, therefore, need not be considered. Coupling factors measured below the critical corona voltage-checked calculations that assumed the actual earth's surface to be the effective ground plane. These measurements were made over soil whose resistivity was as high as 600 meter ohms. It is thought that the actual surface should apply for such electrostatic phenomena except for very high values of earth resistivity.

Measurements were also made of the critical flashover of rod gaps connected between the ground wire and floating phase conductor. The critical breakdown values of such gaps under these conditions were found, over the range of voltages studied, to be essentially the same as with the conductor grounded. These data also showed that, at heights of 30 feet or more above the earth, the influence of the earth plane is negligible. For symmetrical gap electrodes, breakdown is independent of polarity and

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close to the positive polarity breakdown of standard rod gaps.

A configuration consisting of a third ground wire placed below the phase conductors for increased coupling was also studied. Although the coupling for all measured voltages was greater than without the third ground wire, it did not increase as rapidly with voltage as calculations assuming the effective corona radii found for only one or two ground wires would indicate. This was thought to be due to mutual shielding produced by the ground wires tending to decrease corona streamers in the space between them.

THE lightning flashover performance of transmission lines provided with either overhead ground wires, or top conductors equipped with deion protector tubes and serving as shielding wires, is determined not only by the line insulation level and the tower top potential produced by a direct stroke but also by the voltage which appears simultaneously on the phase conductors through induction. One of the principal factors determining this induced voltage is the electrostatic coupling with the ground wire. This is commonly represented by the coupling factor K which is the fraction of the ground-wire potential induced by it on the phase conductor. The surge induced in this manner consists initially of only a voltage wave unaccompanied by current flow until a point of discontinuity on the conductor is reached by the propagating wave.[1] With the exception of strokes close to substations, the only discontinuity to cause a reflection and establishment of an accompanying current wave is provided by the line insulators. These, however, are of such low capacity and high impedance that they produce only a minor effect that can usually be neglected. In any refined analysis to treat such points of discontinuity, the initial induced voltage wave must be known and is a fundamental consideration.

Neglecting the effect of corona on the ground wire by virtue of the potential it acquires when struck, the coupling factor can be calculated readily from the geometric configuration of the line.[1] In the past it has been such values that were most commonly used. However, it has been recognized for some time[2,3] that the presence of corona on the ground wire causes the coupling factor to increase considerably with the ground-wire potential. Calculations of the effect of corona have been made by Torok and Ellis.[3] Its effect is to produce a corona sheath around the ground wire and thus increase its effective radius. The corona radius was determined by them on the basis that it would be of such a magnitude for each ground-wire potential and height above earth as to produce a gradient at this radius equal to 30,000 volts per centimeter (the normal critical breakdown value of air). However, actual reliable measurements of the corona effect have not been available.

Description of Measurements

The coupling factor measurements described in this paper were made at the Trafford High-Voltage Laboratory on relatively short lengths of line. The low impedance of long lines makes it impossible to obtain sufficiently high voltages to be of practical value. However, since the coupling factor is determined only by electric and not by magnetic effects, short lengths of line can be used, provided the voltage induced on the floating conductor can be reliably measured by a potentiometer of sufficiently high impedance that it will not influence the conductor voltage. The development of such recording means was the principal difficulty encountered.

For a preliminary study to determine the effect of various controlling factors, measurements were made inside the laboratory on a line 65 feet long with a ground-wire height of ten feet. This readily enabled the changing of the line configuration. For such a short line, the capacity to ground of the floating conductor is of the order of 150 to 200 micromicrofarads,

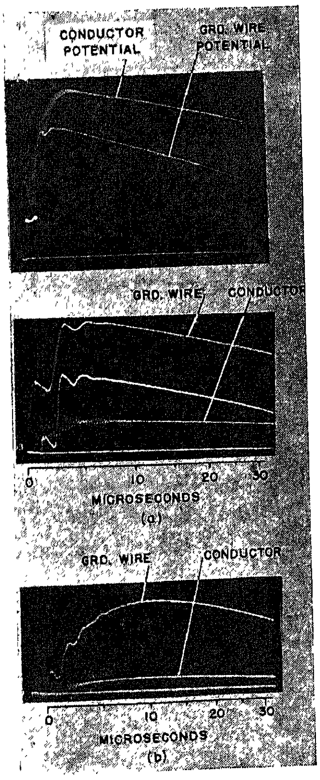


Figure 1. Wave shapes of the ground wire and induced conductor potentials measured on the outdoor line

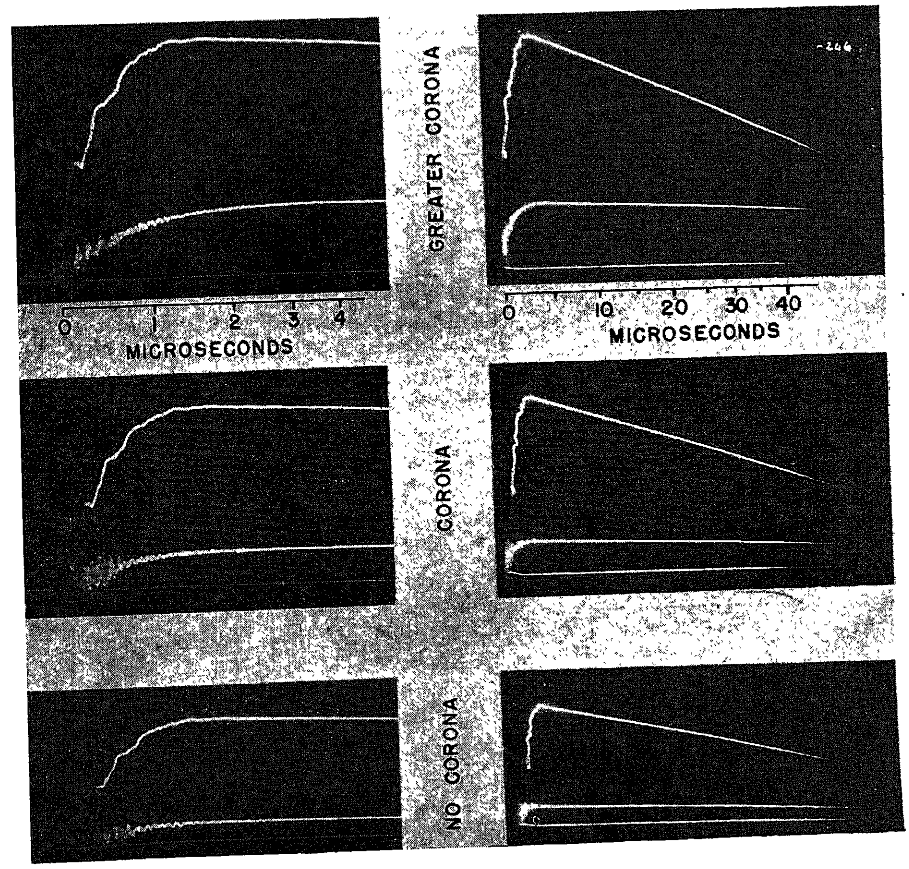


Figure 2. Oscillograms showing effect of corona upon voltage induced on a floating electrode

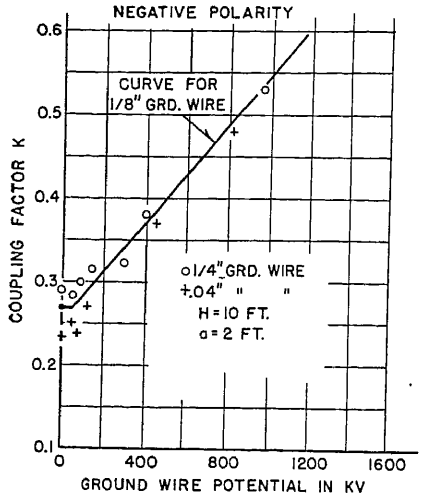


Figure 3. Curve showing that ground-wire size has no significant effect upon coupling factor when corona is present

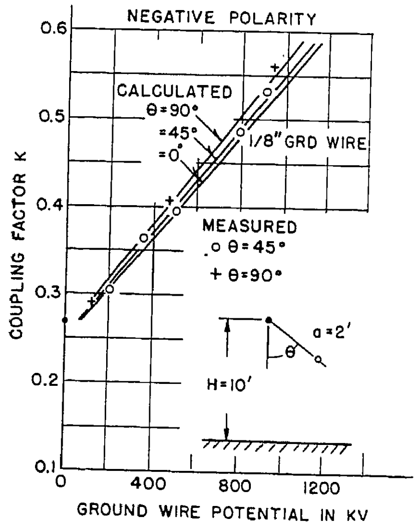
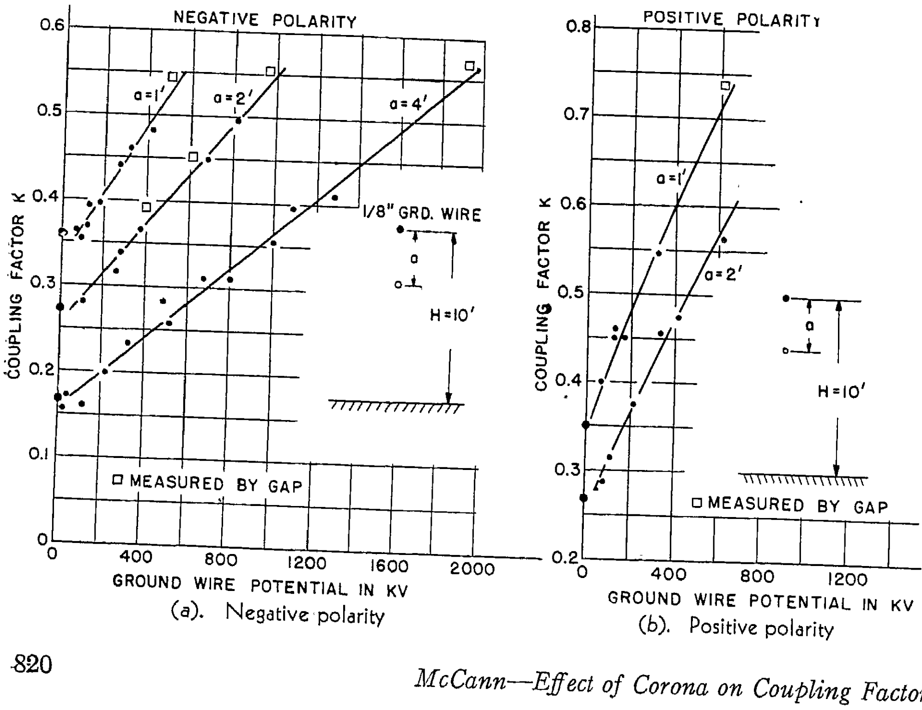


Figure 4. Curve showing that, for one ground wire, c upling factor is determined only by ground-wire potential, its height, and spacing between it and conductor

and it was not found possible to construct a purely capacitive divider of small enough capacitance that could be adequately shielded from the ground wire. A high-resistance potentiometer was developed that did not appreciably load the conductor. It had, however, a time constant of about one microsecond so that fronts of the induced voltage wave, if less than about five microseconds, could not be accurately recorded.

Measurements on lines of practical dimensions were made on a single span providing a conductor length of 350 feet and constructed outside the laboratory over a level section of terrain. Two ground-wire heights 30 and 50 feet were studied. For the mean height of 50 feet a seven foot sag was allowed. For the 30 foot ground-wire height the sag was about five feet. For both the indoor and outdoor lines, the phase conductors were kept equidistant from the ground wire at all points. They were supported from wood poles by long ropes or cords and the ground wire extended at least ten feet past the ends of the conductors for the indoor line and 40 feet for the outdoor line to minimize end effects. The capacity to ground of the floating conductors of the outdoor line was of the order of 700 to 800 microfarads, and a capacity divider was found most suitable. Its coupling with the ground wire did not exceed about ten per cent of the lower values of voltage induced on the conductors, and its capacity was never above about eight per cent of the capacity to ground of the floating conductor. The effects of these factors act in opposite directions, and the recording accuracy is considered to be within five per cent.

Figure 5 (below). Coupling factors as a function of ground wire potential. $H=10$ feet. Single ground wire



(a). Negative polarity

(b). Positive polarity

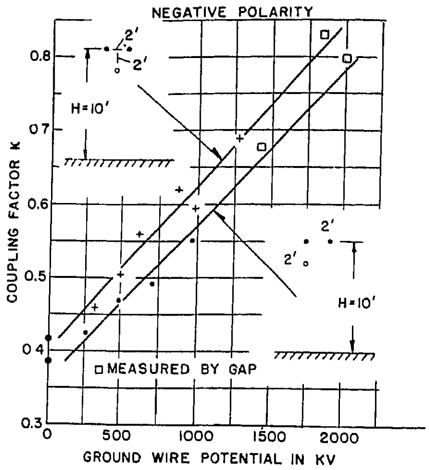


Figure 6. Coupling factors as function of ground-wire potential. $H=10$ feet. Two ground wires. Negative polarity

In Figure 1 are shown typical oscillograms of the voltages measured on the outdoor lines. These were always made at the surge generator end of the ground wire and phase conductor. For the oscillograms of Figure 1*a*, the initial low magnitude crest on the ground-wire voltage is produced by its initial low impedance which is its surge impedance. After a reflected wave propagates back from its far end, it is essentially a capacitor of about 900 micromicrofarads and allows the surge generator to increase the voltage as shown. For wave fronts this short, an accurate record of the front of the induced voltage could not be obtained, because it was necessary to place the cathode-ray oscillograph about 40 feet from the divider and connect them with a low capacity shielded cable. This produced the reflections shown in the conductor voltages of Figure 1*a*. Figure 1*b* shows a ground-wire voltage wave of slower front which was also used and for which the front of the induced voltage could be recorded.

Relation of Induced to Ground-Wire Potential for Different Points on Voltage Wave

To study the effect of corona on the front of the induced voltage wave for steeper wave fronts, a special setup was made inside the laboratory. It consisted of an electrode to which measured voltages were applied with a floating electrode underneath that was connected directly to the plates of the oscillograph only a few feet away. Typical oscillograms obtained both above and below

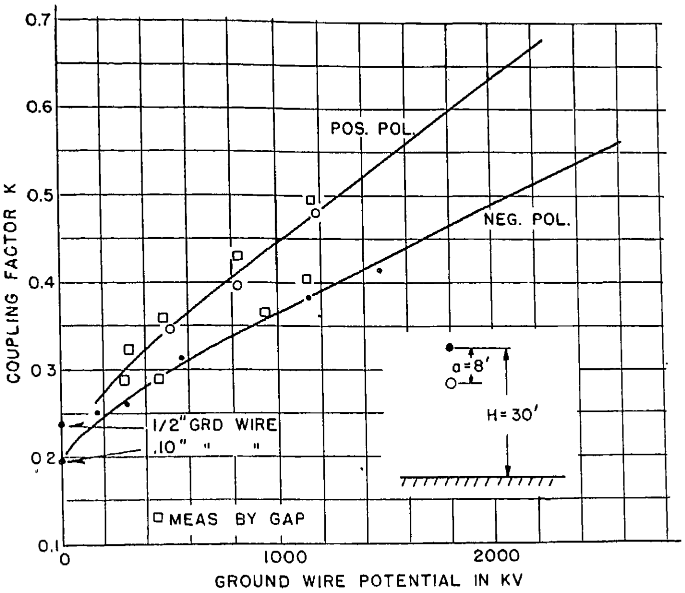
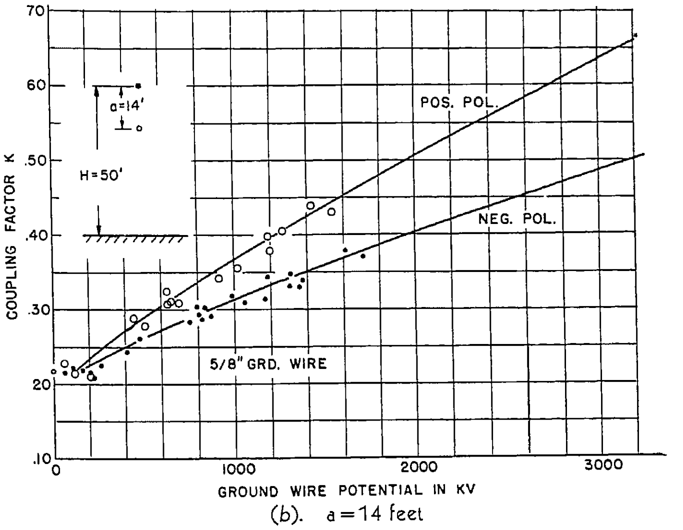
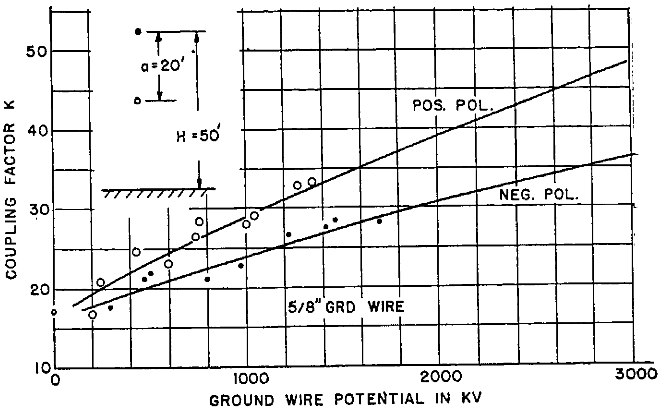


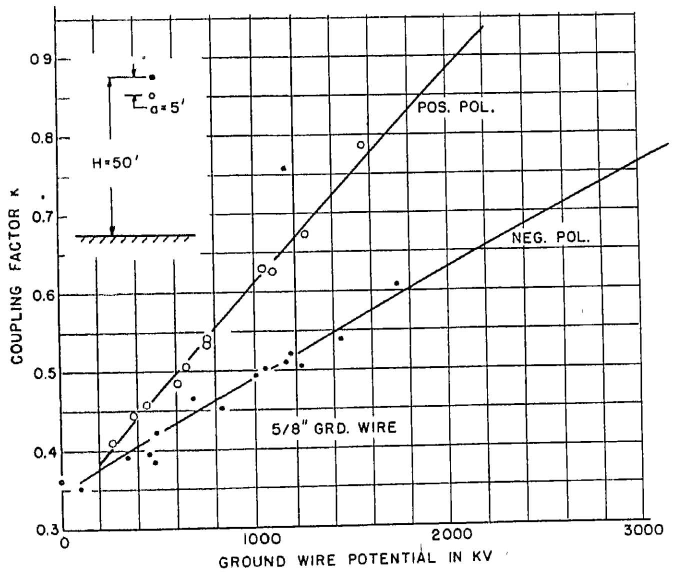
Figure 7. Coupling factors as function of ground-wire potential. $H=30$ feet. One ground wire 0.1 inch in diameter



(*b*). a = 14 feet



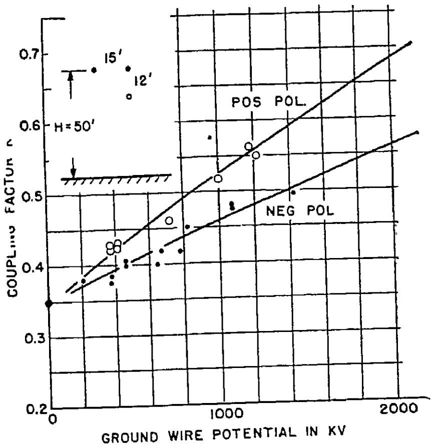
(*c*). a = 20 feet



(*a*). a = 5 feet

Figure 8 (left and above). Coupling factors as function of ground-wire potential. One ground wire. $H=50$ feet

Figure 9a (right). Coupling factors for two ground wires 15 feet apart. $H=50$ feet



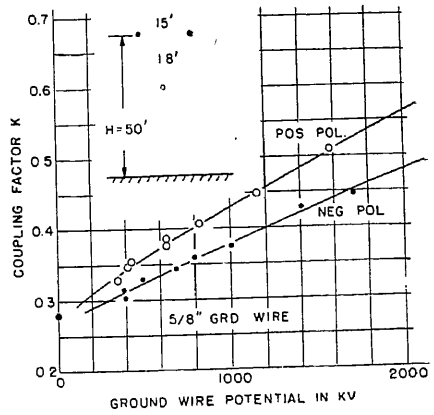


Figure 9b

corona voltage are shown in Figure 2. Comparison of the fronts of the waves at different magnitudes of applied voltage showed that at a given voltage of the upper electrode the induced voltage is essentially the same regardless of the time of occurrence, as long as the comparison is made on the front or crest of the wave. The same result was shown by a study of the slower ten-microsecond front of Figure 1*b* on the outdoor line. For all points on the wave up to its crest, the ratio of the induced voltage to the ground-wire voltage can be represented sufficiently by a coupling factor that is a function only of the ground-wire potential at that particular time.

However, because a small amount of current flows to the conductor when corona is present, the induced voltage wave of a short length of floating conductor does not decrease on the tail of the wave at a rate proportional to the ground-wire potential but is determined by the amount of charge acquired and the leakage impedance of the conductor. This effect is shown in Figures 1 and 2. For a long line where the induced voltage is propagating away from the point on the line contacted by the stroke, the amount of charge is so small as to produce negligible magnetic effects and conductor current, except that the charge should be carried away so rapidly that the tail of the voltage wave induced on the phase conductor by coupling with the ground wire should decrease at a rate determined by a coupling factor varying only with the ground-wire voltage magnitude. Thus, it appears evident that the wave shape and magnitude of the induced conductor voltage can be determined from the ground-wire potential and a coupling factor varying only with ground-wire potential. The higher this potential at any instant the greater the proportion of voltage induced on the conductor. This means, for instance, that the tail of the induced wave on a long line should decrease faster than that of the ground-wire voltage wave. Thus the difference between these voltages, or the voltage wave across the line insulation will have a tail longer than that of the ground-wire potential.

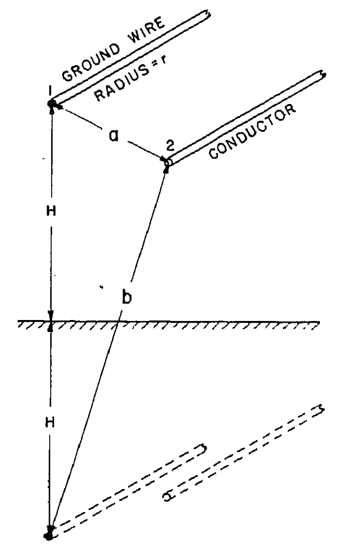


Figure 10. Configuration showing dimensions used in calculating coupling factors for one ground wire

$$\text{Coupling factor } (K) = \frac{\log \frac{b}{a}}{\log \frac{2H}{r}}$$

Measured Coupling Factors

The curves of coupling factor as a function of ground-wire potential for all of the standard ground-wire configurations studied are presented in Figures 3 to 9. Each value of coupling factor is the ratio of the crest induced voltage to the crest ground-wire voltage. Examination of the curves shows that after the ground-wire critical corona voltage is reached, the coupling factor increases rapidly with ground-wire potential. It is also appreciably higher for positive voltages. The maximum voltages which could be produced were about 2,000 kv. Considering that the upper limit of crest stroke current magnitudes is of the order of 150,000 to 200,000 amperes,[4] this provides data for tower top potentials over the full range of stroke currents and for towers having footing resistances up to 15 or 20 ohms. This is the most important range. However, as will be shown later, the data are extrapolated to higher voltages. Before considering the general character of the coupling-factor curves more thoroughly, a number of fundamental considerations will be discussed.

Coupling Factors for No Corona

For the line configuration of each set of coupling-factor curves, the coupling factor has been calculated from standard formulas neglecting corona and assuming the actual surface of the earth as the true ground plane. These values are plotted on the curves at zero potential. For several of the line configurations (see Figures 5*a* and 8*b*) measurements were made below the corona voltage which showed good agreement with the calculated values. There has been considerable controversy in the past as to the location of the effective ground plane in relation to earth resistivity for both electrostatic and magnetic phenomena. These measured data show that both the surface of the floor of the high-voltage laboratory and the ground under the outdoor line act as the effective ground plane for this electrostatic problem. During the period of these tests the earth resistivity under the outdoor line ranged from 400 to 600 meter ohms, as compared to the most common general average value of 100 meter ohms.[5]

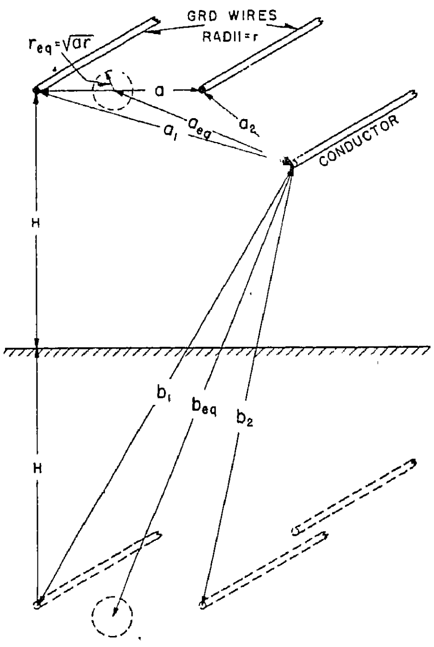


Figure 11. Configuration showing dimensions used in calculating coupling factor for two ground wires

$$\text{Coupling factor } (K) = \frac{\log \frac{\sqrt{b_1 b_2}}{\sqrt{a_1 a_2}}}{\log \frac{2H}{\sqrt{ar}}}$$

Effect of Size of Ground Wire

To determine the effect of the size of both the ground wires and phase conductors, tests were made on the indoor line ($H = 10$ feet) with smooth copper wires 0.04, 0.125, and 0.25 inch in diameter. The size of the conductor should have no effect, and none could be found. The results of tests to show the influence of ground-wire size are shown in Figure 3. Above a ground-wire potential of about 200 kv, no significant difference could be found in the coupling factors for this wide range of wire size. At lower voltages, the coupling factor approaches the calculated values for no corona. It is quite apparent that up to the critical corona voltage the coupling factor is constant, and above this value for the larger wire there is little difference Ground-wire size is thus unimportant in the practical ground-wire voltage range. This was also confirmed by the tests on the outdoor lines. For the ground-wire height H of 50 feet, a five-eighths-inch smooth ground wire was used. For H of 30 feet a 0.1-inch wire was used. The analysis of the effective corona radius to be discussed later will also show that this factor is unimportant.

Presence of Other Phase Conductors

As theoretical considerations indicated, the presence of additional phase conductors had no effect on the induced voltage of a given conductor. The conductor charge picked up from the ground-wire corona was too small to cause any mutual effect.

Effect of Conductor Position With One Ground Wire

Calculations of coupling factor for no corona[1] have shown that for one ground wire and practical conductor configurations, the only important dimension relating the position of the conductor relative to the ground wire is the distance *a* between them (see insert of Figure 4). The data of Figure 4 show that this is also true at high voltages when corona is present.

Gap Flashover Measurements

As a check against the accuracy of the voltage measurements and also to determine the character of flashover conditions on an actual line, data were also obtained on the ground-wire potential necessary to flashover rod gaps connected between the phase and ground wires. These were gaps with one-half inch round electrodes of sufficiently low capacity to produce no appreciable effect on the induced voltage. The points on the coupling-factor curves of Figure 7 indicated as measured by gap flashover were determined from the minimum crest flashover value of the gaps with the conductor first floating and then grounded. The points at highest voltage of Figures 5 and 6 (for $H=10$ feet) were determined from flashover between the ground wire and conductor. The coupling-factor points determined by gap breakdown are from four to eight per cent higher than those determined from measurement of the conductor voltage.

Two factors might cause this difference. The field configuration with the conductor floating and with it grounded may be somewhat different. With it floating, the discharge would usually be expected to start from the ground-wire electrode where the gradient is higher. This should make less difference when the ground wire is positive, since for equal field distortion the discharge will always be initiated from the positive electrode.[6] The other factor is the difference in the wave shape of the voltage across the gap under the two conditions. With the conductor grounded, the gap voltage is the ground-wire voltage which had a tail with 40 microseconds to half value. With it floating, it is (for the short lengths of line) the difference between this and the induced conductor voltage which had a longer tail as explained previously. The greater the ratio of the conductor voltage to the ground-wire voltage, the shorter the tail of the gap voltage wave. The shortest tail for the gap voltage in these tests was about 20 microseconds for which the critical breakdown voltage is about seven per cent above that for a 40 microsecond wave. This would give, therefore, an indicated coupling factor seven per cent too high. The fact that the coupling factor points determined in this way are high by this order of magnitude indicates this to be the principal factor.

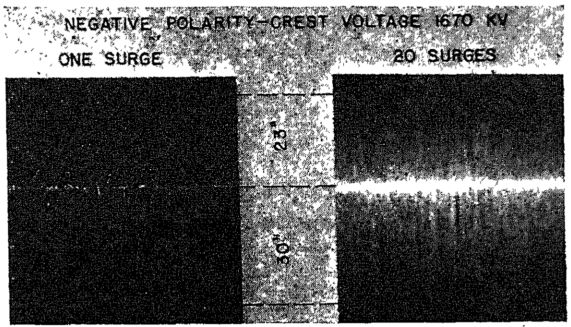
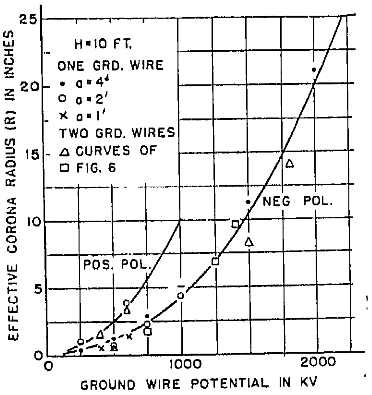


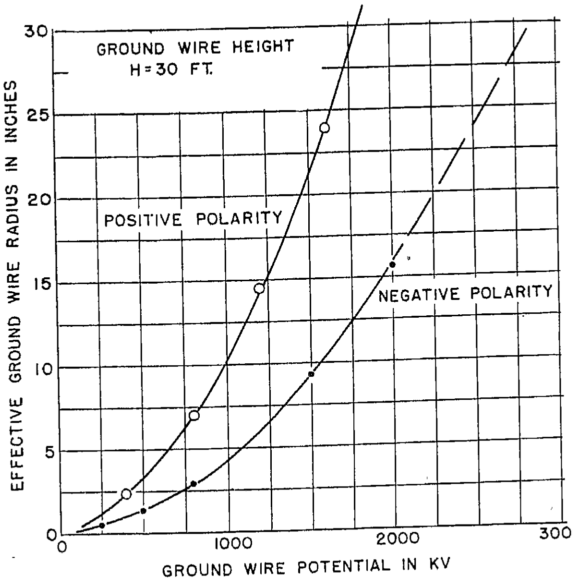
Figure 13. Ground-wire corona produced by surge voltage with $1^1/_2$x40 wave shape. $^1/_8$-inch wire, $H=10$ feet. Effective radius (from Figure 12*a*) is 11.5 inches

It thus is evident that for the range of voltages studied here the probability of line flashover for direct strokes can be based upon the knowledge of the voltage across the insulation (which is the difference between the ground-wire potential and the conductor potential) together with the flashover strength of this insulation determined from tests with the conductor terminal of the insulation grounded.

Effect of Polarity on Gap Breakdown

The field configuration for line insulation is not exactly the same as it is when tested close to ground. It was found that for the flashover tests on the line with $H=30$ feet, the critical breakdown of the gaps was independent of polarity and lay close to the positive polarity breakdown of standard rod gaps on the ground. This is to be expected when the influence of the ground plane is negligible and the gap electrodes are similar. This difference is a secondary effect which cannot be taken into account until more gap breakdown data are available.





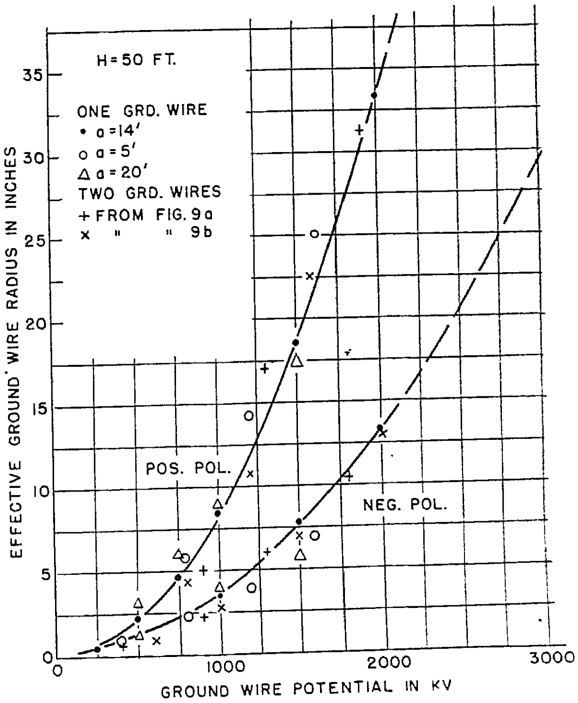


Figure 12. Effective corona radius as function of ground-wire potential

(a above left). $H=10$ feet. Calculated from measured coupling factors of Figures 5 and 6

(b left). $H=30$ feet. Calculated from coupling factors of Figure 7

(c right). $H=50$ feet. Calculated from coupling factors of Figures 8 and 9

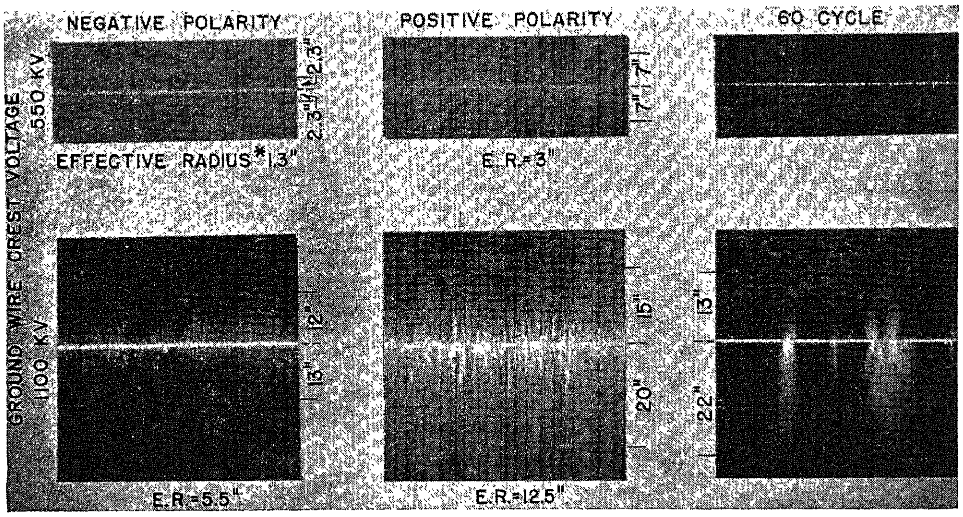


Figure 14. Ground-wire corona showing comparison between 60-cycle and both positive and negative surge voltages. 1/8-inch wire. $H=10$ feet

*Effective radii determined for Figure 12a

Effective Corona Radius

Standard equations for calculating coupling factors when corona is neglected are given in Figures 10 and 11 for the two most common types of ground-wire configuration. For any configuration of conductor and ground wires and ground-wire potential, there is a radius that can be substituted into these equations to give the correct coupling factor. If this "effective radius" is not a function of too many variables, considerable simplification is obtained from using it in the standard equations. Based upon the assumptions of Torok and Ellis,[3] this effective radius is a function only of the ground-wire height H and its potential.

With this thought in mind, values of the effective radius were calculated from the equations of Figures 10 and 11 and the coupling factors of Figures 3 to 9. These are plotted in Figure 12 for each ground-wire height and polarity as a function of ground-wire potential. These curves show that this effective radius is essentially a function of only the ground-wire height and potential. This would be expected for one ground wire as these are the only two factors affecting the field configuration around the wire, and the ground plane would not be expected to exert enough distortion to materially alter the effective center of the corona sheath. As shown by Figures 12*a* and *c* the effective radius for two ground-wire configurations deviates more from the mean curve. This is probably due to the mutual shielding effect of the two wires. However, 15 feet is near the lower limit for their separation in practice, and this effect should decrease with greater separations. Since the coupling factor varies with the logarithm of this radius the variations indicated by Figure 12 are not sufficient to cause more than a few per cent variation in the coupling factors.

VISUAL CHARACTERISTICS OF SURGE CORONA

To determine the visual characteristics of corona produced by impulse voltages and to compare the extent of streamer formation with the effective radii, still photographs were made of the indoor line with the one-eighth-inch ground wire. It was necessary to photograph about 20 discharges to obtain a clear print showing the extent of the streamer development. Figure 13 shows a comparison between one and 20 discharges. Figure 14 shows comparisons between positive and negative surge corona (20 discharges) and 60-cycle corona (three-second exposure).

The distinctive difference between the surge and 60-cycle corona is of particular interest. As has been commonly observed, 60-cycle corona at voltages only moderately above the critical value is a rather uniformly distributed beaded glow. This does not extend as far from the wire as the streamers for either positive or negative surge corona (see records of Figure 14 for 550 kv). For higher 60-

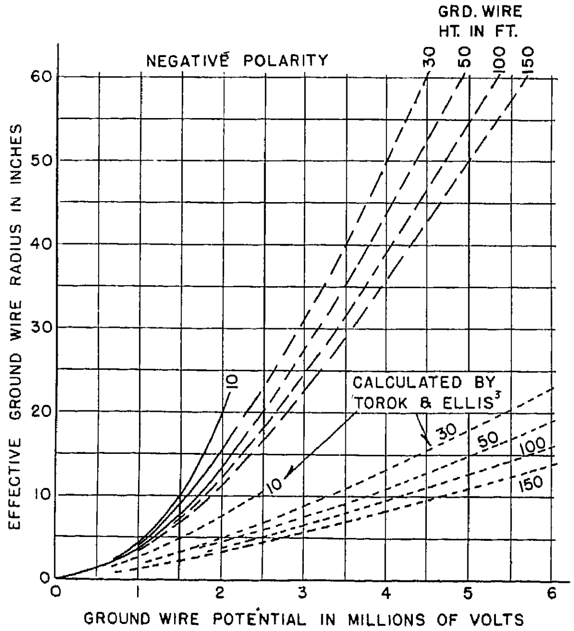
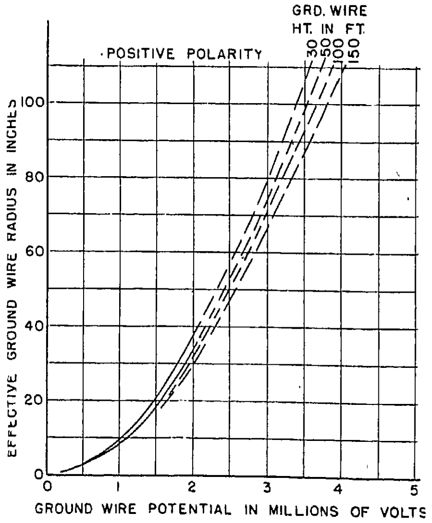


Figure 15

(*a* left). Effective radii for negative polarity compared to values calculated by Torok and Ellis[1]

(*b* right). Effective corona radii for positive polarity



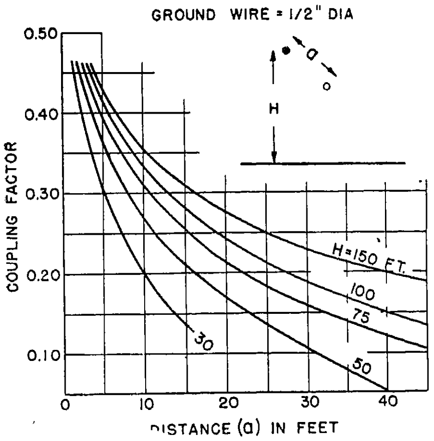


Figure 16. Coupling factors when corona is not present. One ground wire $^1/_2$ inch in diameter

cycle voltages brush discharges of about the same extent as for positive surges are formed (see Figure 14 for 1,100 kv). However, they are bunched at localized points on the wire. This is probably because streamers start first at these points, as the voltage increases relatively slowly on each half cycle, allowing sufficient time for them to develop and shield the other parts of the wire. The streamer formation is much more uniform for surge voltages probably because of the very rapid rate of rise of voltage tending to produce the more nearly instantaneous formation of streamers at all points along the wire. The photograph of Figure 13 for one surge shows about one or two streamers per inch of wire extending a mean distance of about 30 inches toward ground and 23 inches upward for the crest voltage of 1,670 kv. In between these are shorter streamers of varying length that probably did not start to develop as fast as the longer ones and were partly shielded by them.

As shown by Figure 14, the length and density of the positive corona streamers are considerably greater than for negative corona accounting for the greater effective radius for positive corona. Because of the influence of the ground plane, longer streamers are formed toward ground than upward. This dissymmetry increases with voltage. It also should be greater the lower the ground-wire height H. However, even the amount of dissymmetry shown in these photographs for H of ten feet does not cause the effective radius to change materially for the practical range of variation of conductor position. The mean distances that the longer streamers extend downward from the ground wire is roughly twice the effective radii of Figure 12*a* for both polarities.

Extrapolated Curves of Effective Corona Radius

In Figure 15 the effective radii determined from the measured coupling factors are compared to those calculated by Torok and Ellis. They are greater than the calculated values even for negative polarity. They also show somewhat less decrease with ground-wire height and greater rate of increase with ground-wire potential. The variation with ground-wire height is less for positive polarity. In this figure the portions of the curves established by measured data are drawn as solid lines while the extrapolated curves are dashed lines. The extrapolation was based upon the variation of the radii (over the measured range) with ground-wire height, potential, and field gradient. The curves are extrapolated as far as deemed possible with reasonable accuracy. This provides data over the full range of stroke currents for tower footing resistances up to 40 or 50 ohms.

Practical Curves for Determining Coupling Factor

The coupling factor for any practical line configuration can be obtained by substitution of the effective corona radii given by Figure 15 into any of the conventional forms of the coupling-factor equations, such as those of Figures 10 and 11. However, they can be more simply obtained from the curves of Figures 16 to 18. Figures 16 and 17 give values of coupling factor for one and two ground wires of one-half-inch diameter for no corona and are reproduced from reference 1. Figure 18 gives the proper multiplying factors to apply to the values read from these curves to account for corona. Since for the practical range of voltages the coupling factor is independent of actual ground-wire size, the coupling-factor data neglecting corona for any convenient wire size can be used as such a basis with the proper multiplying factors for this size. The analytical

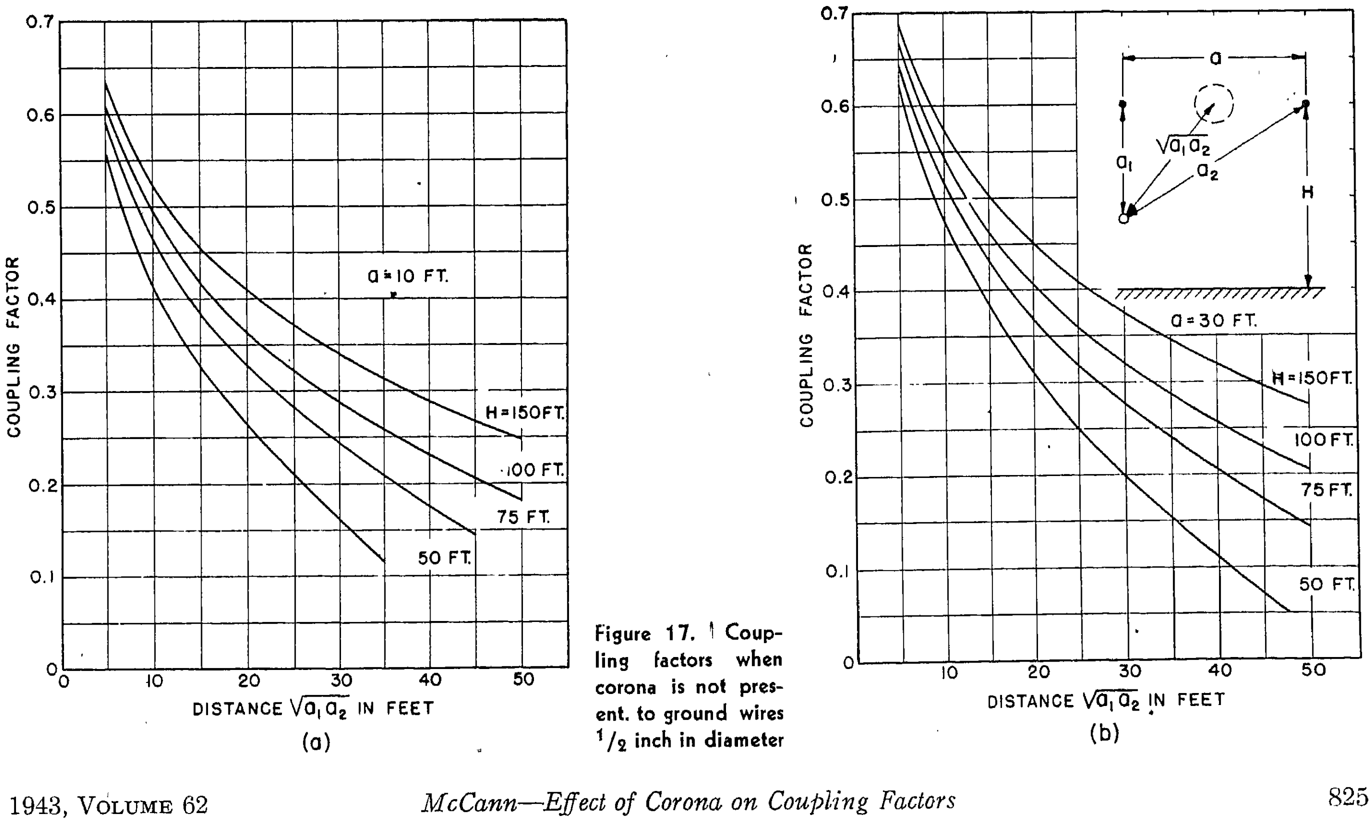


Figure 17. Coupling factors when corona is not present. to ground wires $^1/_2$ inch in diameter

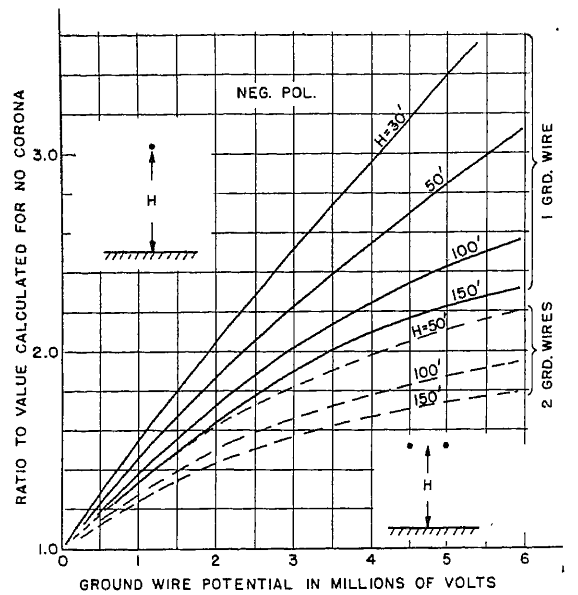
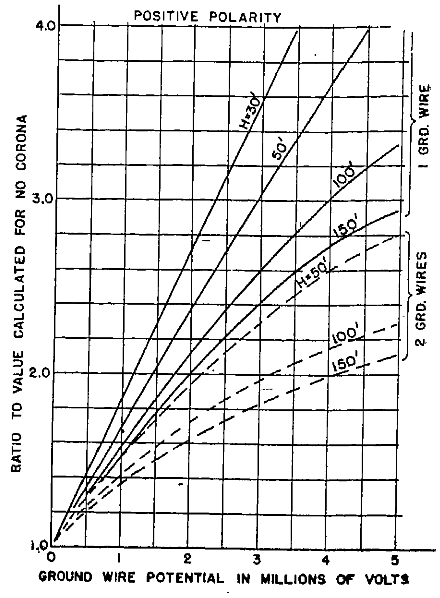


Figure 18. Multiplying factors to use in conjunction with Figures 16 and 17 in determining coupling factors as a function of ground-wire potential

(*a* left). Negative polarity

(*b* right). Positive polarity



character of these multiplying factors is shown by the following equations.

For one ground wire

$$K=\frac{\log b/a}{\log\dfrac{2H}{r(V)}}=\frac{\log b/a}{\log\dfrac{2H}{r_o}}\times\frac{\log\dfrac{2H}{r_o}}{\log\dfrac{2H}{r(V)}}$$

For two ground wires

$$K=\frac{\log\dfrac{\sqrt{b_1b_2}}{\sqrt{a_1a_2}}}{\log\dfrac{2H}{\sqrt{ar(V)}}}=\frac{\log\dfrac{\sqrt{b_1b_2}}{\sqrt{a_1a_2}}}{\log\dfrac{2H}{\sqrt{ar_o}}}\times\frac{\log\dfrac{2H}{\sqrt{ar_o}}}{\log\dfrac{2H}{\sqrt{ar(V)}}}$$

where $r(V)$ is effective radius as a function of voltage r_o is actual radius neglecting corona.

The other symbols are shown in Figures 10 and 11. The multiplying factors are the second terms in these equations. Inspection of the preceding equation for one ground wire shows that the multiplying factor is a function only of the ground-wire height and potential, so that a single curve for each height applies to all spacings between ground wire and conductor. For two ground wires, their spacing a exerts a slight influence, but the deviation of the multiplying factors from the mean curves drawn in Figure 18 is only about five per cent for spacings of a lying between 10 and 40 feet.

The data for negative polarity are, of course, of greatest importance since about 90 per cent of all strokes are entirely of negative polarity[7] and at least 95 per cent have negative polarity for the higher magnitude components.

Increase in Coupling Provided by Additional Ground Wires

The use of another ground wire below the phase conductors (see insert of Figure 19) to provide additional coupling has been advocated by Whitehead[8] and applied with good results. Coupling-factor measurements were made for such a configuration, and the measured values are compared in Figure 19 with those calculated by using the effective corona radii of Figure 15. The actual increase of coupling with voltage is less than calculated. This is probably due to the mutual shielding effect produced by the ground wires in the space between them. However, comparison of the measured coupling factors of Figure 19 with those of Figure 9*a*, where the configuration is identical except for the third ground wire, shows that an increased improvement of coupling for all voltages is provided by the third ground wire. Because of this shielding effect, the effective radii of Figure 15 cannot be used, and each configuration of this type should be studied individually.

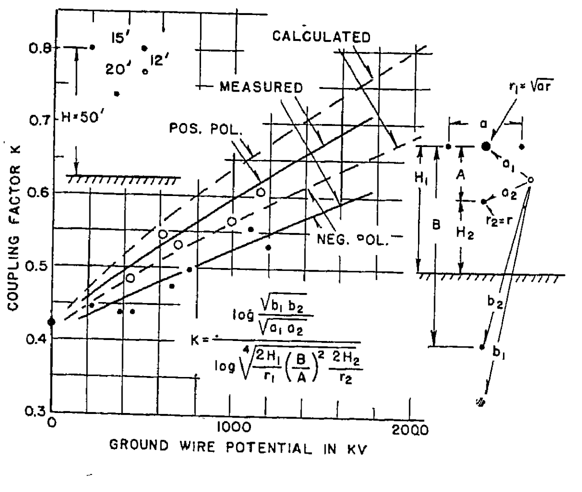


Figure 19. Effect of adding third ground wire to increase coupling

Comparison of measured values of coupling factors with those calculated from effective corona radii given by Figure 12c

References

1. Transmission and Distribution Reference Book, Westinghouse Electric and Manufacturing Company. Chapter 11, page 284.

2. The Effect of Corona on Ground-Wire Protection of Power Transmitting Circuits, E. M. Hunter. *General Electric Review*, February 1930, pages 100–03.

3. Factors Affecting Surge Potentials, J. J. Torok, W. R. Ellis. *Electric Journal*, September 1933, pages 381–7.

4. Lightning Phenomena—III, C. F. Wagner, G. D. McCann. Electrical Engineering, volume 60, October 1941, pages 483–500.

5. Symmetrical Components (book), C. F. Wagner, R. D. Evans. McGraw-Hill Book Company, Inc., New York, N. Y. Page 148.

6. Shielding of Transmission Lines, C. F. Wagner, G. D. McCann, G. L. MacLane, Jr. AIEE Transactions, volume 60, 1941, pages 313–28.

7. Field Disturbances Produced by Lightning, G. D. McCann, D. E. Morgan. AIEE Transactions, volume 62, 1943, pages 345–67.

8. Design Transmission Lines for Inherent Lightning Protection, E. R. Whitehead. *Electric Light and Power*, September 1942, page 32.

Pole-Face Loss in Solid-Rotor Turbine Generators

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ONE of the primary problems which confront the designer of electrical machinery is the predetermination of losses. This is peculiarly true for the designer of high-speed turbine-driven generators; the losses must be kept low, not only to increase the efficiency but also to keep the size of the apparatus to a minimum.

Losses are usually classified as

(*a*). Ventilation, windage, and friction.
(*b*). Open-circuit core losses.
(*c*). Armature and field copper loss.
(*d*). Load loss.

It is with one component of this last group of losses that this paper is concerned.

The term "load loss," as usually employed, includes all extra losses coincident with the operation of the machines which are not included in the other items. In test it is usually determined as the difference between the actual losses on short-circuit loss test and the more readily determined components of windage, friction, copper loss, and so forth.

In solid-rotor turbine-driven revolving field generators, one of the largest components of load loss is caused by the existence of eddy currents in the surface of the rotor. These eddy currents are caused by the armature-coil currents. The balanced multiphase current of fundamental frequency which flows in the armature produces a magnetomotive force which rotates synchronously with the rotor. It also produces, because of the imperfect distribution of the armature coils, magnetomotive forces which move with respect to the rotor, causing eddy currents and losses in the rotor surface. Unbalanced armature phase currents and armature currents of other than fundamental frequency can also give rise to these losses.

This paper concerns itself with the development of methods whereby the designer may predetermine the amounts of these pole-face losses.

In Appendix A the general mathematical relations governing the phenomena are written; equations for the loss are presented in forms which permit reasonable facility in calculation. Appendix B presents methods of determining winding factors which are fundamental to the determination of losses. Sample calculations are given in Appendix C. A brief list of references is added for those who are interested in a further study of various aspects of the problem. A summary of the results of the investigation, a recapitulation of the assumptions in the solutions presented, a discussion of the limitations of the method, and a list of nomenclature follow.

Results

It is found that the loss attributed to any harmonic of armature magnetomotive force which moves relative to the rotor may be written

$$\text{Loss} = (1/2) I_{on}^2 (\text{area}) 4\pi 10^{-9} l f R \qquad (19)$$

An equivalent form, using quantities with which the designer is more familiar is given by equation 24. Losses caused by each of several coexistent harmonics are added to obtain the total loss.

The accuracy of the calculation for loss depends on the applicability of the assumptions and on the evaluation of the factor R. Use of the method indicates that the assumptions are valid for the usual case and that for the extremes where rotor end effects or magnetic saturation are appreciable, means are available to mitigate the inaccuracies attributable to these causes. The factor R may be determined from:

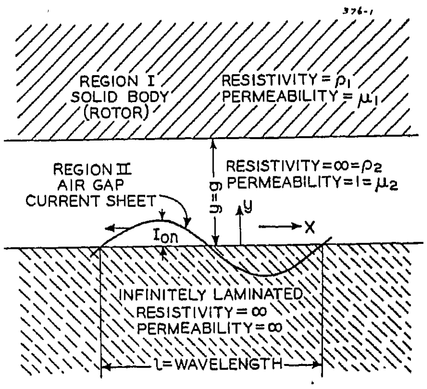

Figure 1. Idealized system

The current sheet on the stator magnetomotive-force wave moves relative to the rotor causing eddy currents and losses there. See Appendix A

1. Equation 22.
2. Figure 2, where a minor limitation is placed on the value of permeability as explained in the text following equation 22.
3. Figure 3, which, in effect, assumes a permeability such that loss is maximum. Figure 3 may be used with good accuracy for the determination of those losses in turbine generators of usual design which are caused by armature magnetomotive force harmonics of the fifth to the fifteenth order. Experience has shown that substantially the entire pole-face loss in most machines is caused by harmonics within these limits.

Assumptions

The major assumptions used in this paper to simplify the determination of pole-face losses are summarized:

1. The fundamental solution is made in terms of rectangular, rather than cylindrical co-ordinates.
2. End effects are neglected; the rotor is assumed to be infinitely long.
3. The armature magnetomotive force of each harmonic order is represented in the calculations by a current sheet on the stator surface of sinusoidally varying density, which produces the same value of magnetomotive force in the air gap for that harmonic as the actual windings in the slots.
4. The resistivity and permeability of the materials are constant. Stator permeability and resistivity are assumed infinite.
5. The losses of all harmonics can be superposed; the total loss is the sum of the losses determined for each harmonic.
6. Discontinuities in the rotor surface because of wedges, and so forth, are neglected (but the value of permeability may be chosen to make allowances for this condition).
7. In utilizing the equations developed, maximum permeability is limited by the tangential component of flux in the rotor surface. (See Appendix C.)

Conclusion and Discussion

The methods of determining pole-face loss in turbine generators which are presented here are simple and reasonably accurate. For machines of normal design they make possible a determination of loss within a few minutes after winding factors are known; for exceptional cases, the accuracy of the method is less, and the calculations are not simple, but the utility is great.

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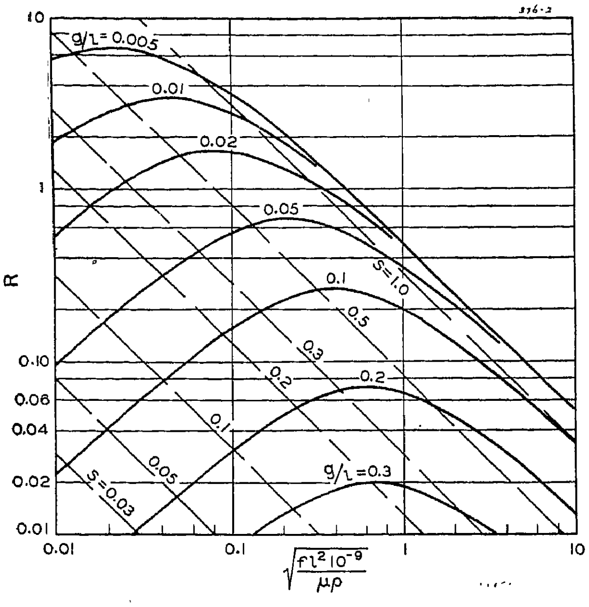

Figure 2. Plot of the resistance factor R and the flux density factor S with the parameters shown

See paragraph following equation 22

It has been gratifying, after investigating mathematically these complex phenomena, to find that the calculation of losses by means of relatively simple equations, without resort to empirical constants for the most frequently encountered cases, is corroborated by test results.

One fact which is apparent from a review of the data presented in this paper is that, for three-phase machines of usual design, high pole-face losses most often arise from the fifth and seventh magnetomotive force harmonics. It is evident from Figure 3 that the low harmonics are the most powerful in increasing the factor R. A glance at the winding constants of Table I, together with equation 24, will show that the fifth and seventh harmonics are most usually the cause of high pole-face losses. This is further illustrated by the third sample calculation of Appendix C.

It is interesting to note also that a winding with two-thirds pitch is by no means best from a standpoint of pole-face loss. In this connection it may be pointed out that the specification of the coil spacing on the rotor, together with the winding factors, determines the telephone influence factor of the open-circuit voltage of the machine. It is evident from the preceding remarks that any preference for a stator winding of given pitch, such as two-thirds pitch, except where all factors are adequately studied and quantitatively understood, is unwarranted.

The paper presents not only equations for calculating the losses, but also equations for determining the pitch and distribution factors and the effects of phase groupings of armature coils. This extension of the fundamental purpose of the paper was made so that the designer might have at hand all the tools with which to treat most cases.

A number of writers, in published and unpublished work, has treated the subject of induced losses in solid materials. The singular contribution of this paper is believed to be the framing of the results so that they can be readily applied in the design of turbine-driven generators.

Nomenclature

Centimeter-gram-second units are used, except where otherwise stated. Electrical units are in the "practical" system. English units are used in the sample calculation and in equation 24; the parts of the report probably most directly useful to the designer.

A, B, C—Arbitrary constants.
A_n—"Square wave" value of armature reaction, nth harmonic.
a—$2\pi/l$.
b—$\sqrt{a^2+j\,\frac{4\pi\mu\omega 10^{-9}}{\rho}}$ (also coil side width in Appendix B).
C—Number of coils in a group (Appendix B).
D—Gap diameter.
D_n—Factor proportional to $\frac{k_{pn}k_{dn}}{n}$.
E—Voltage.
f—Frequency; f_r, frequency in rotor.
g—Gap length.
H—Magnetic intensity.
I_{on}—Density of current sheet for nth harmonic.
I_m—Current of harmonic frequency m.
i—Current density.
j—$\sqrt{-1}$.
$k_{pn}k_{dn}$—Pitch and distribution factors for nth space harmonic, <1.
L—Active machine length (inches).
l—Wave length.
m—Order of time harmonic.
n—Order of space harmonic.
P—Number of poles.
p—Pitch.
Q—Number of phases, q = order of phase (Appendix B).
q, q_i, q_r—See equation 21.
R—Resistance factor (equation 20).
S—Magnetic intensity factor (equation 25).
t—Time.
x, y, z—Co-ordinates (Figure 1).
β—Flux density (or electrical slot angle in Appendix B).
γ—Angle between coil groups.
θ—Electrical angle (Appendix B).
μ—Permeability.
ρ—Resistivity.
ω—$2\pi f$.

Appendix A. Development of the Equations for Loss

This appendix is divided into three sections. In the first, the region of the air gap is analyzed mathematically to determine the relations between the fundamental electrical quantities.* In the second section the equations governing the loss are written from those developed in the first section. The third section contains the development of equations for the magnetic intensity at the rotor surface. In some cases, a knowledge of this magnetic intensity is necessary because the loss is a function of the permeability of the rotor steel and the permeability depends on intensity of the magnetic field.

Fundamental Relations

The region of the air gap may be idealized as shown on Figure 1. One of the harmonics of stator magnetomotive force which moves with respect to the rotor is represented as a current sheet of infinitesimal thickness on the stator surface, which has wave length l and a maximum value of I_{on} per unit length

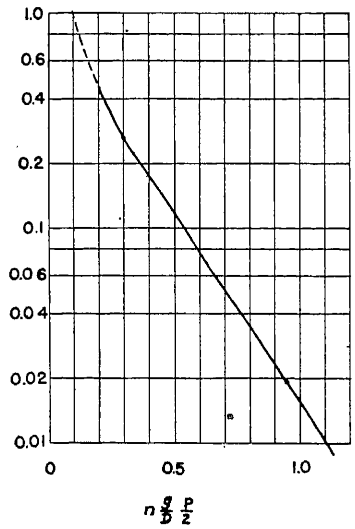

Figure 3. Simplest manner of determining R

For limitations see discussion following equation 22 of sheet. The stator is represented as a perfectly laminated semi-infinite solid of infinite permeability and resistivity. The rotor (region 1) at a distance $y=g$ from the stator, is represented by a semi-infinite solid of permeability $\mu=\mu_1$ and resistivity $\rho=\rho_1$. The air gap, region 2, has permeability $\mu=\mu_2=1$, resistivity $\rho=\rho_2=\infty$.

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* The method of analysis was outlined to the writer by Dr. H. Poritsky.

Maxwell's equations, in vector form, are written below.

$$\nabla\times\overline{H}=0.4\pi\bar{\imath} \qquad \rho\bar{\imath}=\bar{E}$$

$$\nabla\times\bar{E}=-10^{-8}\frac{\partial\beta}{\partial t} \qquad \bar{\beta}=\mu\overline{H} \tag{1}$$

Since the only voltage existent is that in the Z-direction, the second part of equation 1 reduces to

$$\frac{\partial E_z}{\partial y}=-10^{-8}\mu\frac{\partial H_x}{\partial t} \quad \frac{\partial E_z}{\partial x}=10^{-8}\mu\frac{\partial H_y}{\partial t} \tag{2}$$

Combination of equations 2 and 1 yields

$$\frac{\partial^2 E_z}{\partial x^2}+\frac{\partial^2 E_z}{\partial y^2}=\frac{4\pi\mu 10^{-9}}{\rho}\frac{\partial E_z}{\partial t} \tag{3}$$

The solutions for current and voltage, and so forth, are harmonic; they may be written:

$$\begin{aligned} i_z&=i_z'\epsilon^{j(\omega t+ax)}\\ E_z&=E_z'\epsilon^{j(\omega t+ax)}\\ H_x&=H_x'\epsilon^{j(\omega t+ax)}\\ H_y&=H_y'\epsilon^{j(\omega t+ax)} \end{aligned} \tag{4}$$

Thus equations 2 and 3 become

$$\frac{\partial E_z}{\partial y}=-j10^{-8}\mu\omega H_x$$

$$\frac{\partial E_z}{\partial x}=jaE_z=j10^{-8}\mu\omega H_y$$

$$-a^2E_z+\frac{\partial^2 E_z}{\partial y^2}=\frac{j\omega 4\pi\mu 10^{-9}}{\rho}E_z \tag{5}$$

The controlling differential equation is written from equation 5,

$$\frac{\partial^2 E_z}{\partial y^2}=b^2E_z \tag{6}$$

where

$$\begin{aligned} b^2&=a^2+\frac{j4\pi\omega\mu 10^{-9}}{\rho}\\ &=\left(\frac{2\pi}{l}\right)^2+\frac{j4\pi\omega\mu 10^{-9}}{\rho} \end{aligned} \tag{7}$$

The solution may be written in the form, for region 1,

$$E_{z1}=C\epsilon^{-b_1(y-g)} \tag{8}$$

for region 2,

$$E_{z2}=A\cosh a(y-g)+B\sinh a(y-g) \tag{9}$$

The constants A, B, and C are fixed by the conditions across the boundary between regions 1 and 2,

E_z is continuous

$$\frac{1}{\mu}\frac{\partial E_z}{\partial y} \text{ or } \frac{\partial H_x}{\partial t} \text{ is continuous} \tag{10}$$

and at $y=0$,

$$\frac{\partial E_z}{\partial y}=j4\pi 10^{-9}\mu_2\omega I_{on}$$

The resulting equations are written below

$$E_{z1}=-jI_{on}4\pi 10^{-9}lf\,\frac{\epsilon^{-b_1(y-g)}}{\sinh ag+\dfrac{b_1}{a\mu_1}\cosh ag} \tag{11}$$

$$E_{z2}=-jI_{on}4\pi 10^{-9}lf\times\frac{\cosh a(y-g)-\dfrac{b_1}{a\mu_1}\sinh(y-g)}{\sinh ag+\dfrac{b_1}{a\mu_1}\cosh ag} \tag{12}$$

At the current sheet, where $y=0$, the impedance (per square centimeter) met by the current is

$$Z=-\frac{E_z}{I_{on}}\bigg|_{y=0}=j4\pi 10^{-9}lf\times\frac{1+\dfrac{b_1}{a\mu_1}\tanh ag}{\tanh ag+\dfrac{b_1}{a\mu_1}} \tag{13}$$

The loss may be written from equation 13

$$\text{Loss}=\left(\frac{I_{on}^2}{2}\right)\text{(area of current sheet)}\text{(real part of }Z) \tag{14}$$

Equation 14 will be stated in more convenient forms in a subsequent section. Before this is done, the general equations for the magnetic intensities will be written.

The equations governing the magnetic intensities may be written directly from equations 2, 11, and 12.

In region 1 (rotor iron)

$$H_{X1}=-0.4\pi I_{on}\frac{b_1}{a\mu_1}\times\left[\frac{\epsilon^{-b_1(y-g)}}{\sinh ag+\dfrac{b_1}{a\mu_1}\cosh ag}\right] \tag{15}$$

$$H_{y1}=-j0.4\pi I_{on}\frac{1}{\mu_1}\times\left[\frac{\epsilon^{-b_1(y-g)}}{\sinh ag+\dfrac{b_1}{a_1}\cosh ag}\right] \tag{16}$$

In region 2 (air gap)

$$H_{X2}=0.4\pi I_{on}\times\left[\frac{\sinh a(y-g)-\dfrac{b_1}{a\mu_1}\cosh a(y-g)}{\sinh ag+\dfrac{b_1}{a\mu_1}\cosh ag}\right] \tag{17}$$

$$H_{y2}=-j0.4\pi I_{on}\times\left[\frac{\cosh a(y-g)\ \dfrac{b_1}{a\mu_1}\cosh a(y-g)}{\sinh ag+\dfrac{b_1}{a\mu_1}\cosh ag}\right] \tag{18}$$

The Equations for Loss

Equation 14 may be written

$$\text{Loss}=\frac{I_{on}^2}{2}\text{(area of inside stator surface)}\,4\pi 10^{-9}lfR \tag{19}$$

The loss is in watts if other units are in the centimeter-gram-second system. R is defined by

$$R=\text{real part of } j\left[\frac{1+\dfrac{b_1}{a\mu_1}\tanh ag}{\tanh ag+\dfrac{b_1}{a\mu_1}}\right] \tag{20}$$

To obtain a more suitable expression for R, let

$$q_i=\text{imaginary part of }\frac{b_1}{a\mu_1}$$

$$q_r=\text{real part of }\frac{b_1}{a\mu_1}$$

$$\frac{b_1}{a\mu_1}=\sqrt{\frac{1}{\mu_1^2}+j\frac{l^2\omega 10^{-9}}{\pi\rho_1\mu_1}} \tag{21}$$

A combination of equations 20 and 21 yields

$$R=\frac{q_i(1-\tanh^2 ag)}{(\tanh ag+q_r)^2+q_i^2} \tag{22}$$

The preceding expression for R is general and holds for all conditions under the general assumptions of the analysis. Under most conditions encountered in the determination of pole-face losses, an examination of equations 21 shows that $q_r=q_i$. Under these conditions it is practicable to make a plot of R against a factor proportional to $l^2\omega/\rho\mu$. It is assumed that μ_1 is relatively large.

A plot of R, subject to the conditions stated in the preceding paragraph, can be made, similar to that illustrated in Figure 2. Subscripts are omitted, but it is to be understood that l is the wave length of the harmonic under consideration, and f is the frequency of the eddy currents generated in the rotor surface.

It will be noticed in reviewing Figure 2, that lines of constant g/l have maxima within the region plotted. In many of the calculations made by the writer, it has been found that the parameters are so related that the factor R is found at or near the peaks of the constant g/l contours.

This fact permits a further simplification. The values of the maxima may be plotted against g/l or an equivalent parameter. This has been done in Figure 3. The actual parameter chosen is $n(g/D)(P/2)$.

It is evident that Figure 3 shows the maximum possible value of the factor R for any harmonic. As mentioned previously, it has been found that the factor R falls at or near these maxima for many cases encountered in turbine generators of usual design. Departures occur at very low harmonics (the upper part of the curve) and at very high harmonics (below the range of the plot).

In applying equation 19 to the determination of loss attributable to the harmonic magnetomotive forces produced by currents of fundamental frequency in the armature, it is convenient to write the equation in terms of units more commonly used in design. The final equation results from application to equation 19 of the equations below and a departure from the centimeter-gram-second system.

$$\frac{A_n}{A_1}=\frac{1}{n}\frac{k_{pn}}{k_{p1}}\frac{k_{dn}}{k_{d1}}$$

$$A_n=\frac{l}{8}I_{on}=\frac{\pi}{4}\frac{D}{pn}I_{on}\text{ (consistent units)} \quad (23)$$

The resulting equation may be written:

$$\text{Loss (kw)}=\left(\frac{A_1}{10{,}000}\right)^2\times$$
$$\left(\frac{k_{pn}k_{dn}}{k_{p1}k_{d1}}\right)^2\left(\frac{P}{2}\right)\left(\frac{L}{10}\right)\left(\frac{fr}{n}\right)R \quad (24)$$

where

A_1 = square-wave value of fundamental armature mmf
= $\pi/4\times$peak value of fundamental armature mmf
$k_{pn}k_{dn}$ = pitch and distribution factors for nth harmonic of mmf, <1
P = number of poles
L = rotor length, inches
f_r = frequency of currents generated in the rotor attributable to nth mmf harmonic
R = factor from Figure 2 or Figure 3 or equation 22

In the preceding equation a "winding factor" is not included; for example, the fact that a symmetrical three-phase winding with symmetrical phase current does not allow the presence of a third harmonic magnetomotive force in the air gap is considered to be so well known that a factor to account for this phenomenon is not included in the aforementioned equation.

Magnetic Intensity and Permeability

From a consideration of the equations for loss and of Figure 2 it is evident that, for some calculations of loss, the loss may vary either directly or inversely as the square root of the rotor permeability. Since the permeability is a function of the magnetic intensity, a simple method of determining this factor is desirable.

As an index from which a value of rotor permeability can be estimated, it has been considered to be sufficient to determine the maximum value of H_x at the rotor surface. For almost all cases, this component of magnetic intensity is considerably larger than H_y, and it indicates reasonably well the degree of saturation of the surface.

Thus the present problem may be reduced to the determination of the absolute value of equation 15. Similar to the development of the equation for the resistance factor R, equation 22, we can define a magnetic intensity factor, S, such that

$$\text{Maximum value of } H_x|_{y=g}=I_{on}S \quad (25)$$

where

$$S=\frac{0.4\pi}{\cosh ag}\left[\frac{\sqrt{q_i^2+q_r^2}}{(\tanh ag+q_r)^2+q_i^2}\right] \quad (26)$$

This factor may be plotted together with R in Figure 2 as shown, placing the same limitation of the relation between q_i and q_r as is used in the plot of R.

The use of the preceding equations is illustrated in Appendix C.

Appendix B

Determination of the Harmonic Magnetomotive Forces

The harmonic components of the armature magnetomotive force may be said to depend on the coil pitch factor, the distribution factor, the relationship of the coils in the several phase groups, and the currents in the armature.

The Pitch Factor, K_{pn}

If the current in a single coil is assumed to flow in filaments on the stator surface, the distribution of the radial component of magnetomotive force attributable to the current in the single coil may be represented by the solid line in Figure 4.

It is readily established that the coefficients of the harmonic terms of Fourier Series representing this solid line are

$$C_n=\frac{1}{\pi}\int_{\pi}^{\pi}f(\theta)\cos n\theta d\theta$$
$$=\frac{2}{n\pi}\left[\sin\frac{np\pi}{2}\right]=\frac{2}{n\pi}k_{pn} \quad (27)$$

The pitch factor, k_{pn}, is defined by the factor in the brackets.

It appears that for the lower harmonics the assumption of filamentary currents probably causes little error. For the higher harmonics this may not be true, and greater accuracy may be obtained by assuming that the current flows in a ribbon on the stator surface. If this ribbon has width, b, electrical angle, the magnetomotive force distribution may be represented by the dotted variation of the full line of Figure 4. Similar

Table I. $(K_{pn}K_{dn})/(K_{p1}K_{d1})$ for Pitch and Distribution Shown

42 Slots Per Pair of Poles

Definitions of Symbols in Appendix B

	Pitch	18/21	17/21	16/21	15/21	14/21	13/21	12/21	11/21	10/21
Distribution (7) (7)										
$K_{p1}K_{d1}$	1	0.930	0.911	0.888	0.859	0.826	0.788	0.744	0.700	0.652
$\frac{K_{pn}K_{dn}}{K_pK_{d1}}$	1	1	1	1	1	1	1	1	1	1
	3	0.540	0.440	0.315	0.167	0	0.182	0.376	0.573	0.774
	5	0.091	0.016	0.064	0.141	0.204	0.244	0.255	0.230	0.168
	7	0	0.078	0.139	0.166	0.150	0.091	0	0.102	0.190
	9	0.108	0.226	0.251	0.166	0	0.182	0.300	0.295	0.153
	11	0.081	0.105	0.061	0.026	0.102	0.117	0.056	0.052	0.138
	13	0.091	0.069	0.014	0.091	0.091	0.008	0.091	0.118	0.037
	15	0.166	0.039	0.139	0.166	0	0.181	0.167	0.051	0.232
	17	0.063	0.030	0.084	0.020	0.078	0.069	0.043	0.105	0.0159
	19	0.034	0.066	0.055	0.052	0.076	0.034	0.095	0.007	0.111
	21	0	0.157	0	0.167	0	0.182	0	0.209	0
Distribution (1′-5′-1) (1′-5′-1)										
$K_{p1}K_{d1}$	1	0.912	0.893	0.870	0.842	0.810	0.772	0.729	0.686	0.636
$\frac{K_{pn}K_{dn}}{K_{p1}K_{di}}$	1	1	1	1	1	1	1	1	1	1
	3	0.441	0.359	0.258	0.136	0.008	0.149	0.307	0.467	0.634
	5	0.044	0.007	0.031	0.068	0.098	0.117	0.122	0.110	0.081
	7	0	0	0	0	0	0	0	0	0
	9	0.026	0.055	0.061	0.040	0	0.044	0.073	0.072	0.037
	11	0.095	0.123	0.072	0.031	0.119	0.137	0.065	0.061	0.162
	13	0.159	0.122	0.024	0.160	0.159	0.013	0.160	0.208	0.066
	15	0.383	0.090	0.320	0.383	0	0.417	0.385	0.117	0.536
	17	0.170	0.083	0.227	0.053	0.212	0.187	0.117	0.285	0.043
	19	0.102	0.197	0.166	0.154	0.227	0.103	0.283	0.022	0.333
	21	0	0.469	0	0.509	0	0.555	0	0.625	0
Distribution (3-3) (3-3)										
$K_{p1}K_{d1}$	1	0.920	0.902	0.878	0.850	0.818	0.780	0.736	0.692	0.642
$\frac{K_{pn}K_{dn}}{K_{p1}K_{d1}}$	1	1	1	1	1	1	1	1	1	1
	3	0.497	0.404	0.290	0.154	0	0.168	0.346	0.527	0.715
	5	0.029	0.005	0.020	0.044	0.064	0.077	0.080	0.072	0.053
	7	0	0.185	0.329	0.392	0.353	0.214	0	0.241	0.450
	9	0.207	0.434	0.483	0.318	0	0.348	0.576	0.567	0.295
	11	0.236	0.305	0.178	0.077	0.294	0.340	0.162	0.150	0.403
	13	0.071	0.054	0.011	0.071	0.071	0.006	0.071	0.092	0.029
	15	0.020	0.005	0.017	0.020	0	0.022	0.020	0.006	0.028
	17	0.068	0.033	0.091	0.021	0.084	0.075	0.046	0.114	0.017
	19	0.120	0.232	0.196	0.182	0.267	0.121	0.334	0.025	0.392
	21	0	0.369	0	0.392	0	0.427	0	0.481	0
Distribution (6-) (6-)										
$K_{p1}K_{d1}$	1	0.942	0.923	0.898	0.870	0.837	0.798	0.754	0.708	0.657
$\frac{K_{pn}K_{dn}}{K_{p1}K_{d1}}$	1	1	1	1	1	1	1	1	1	
	3	0.606	0.493	0.354	0.1877	0	0.204	0.423	0.642	0.872
	5	0.164	0.028	0.116	0.2538	0.367	0.438	0.459	0.414	0.303
	7	0	0	0	0	0	0	0	0	0
	9	0.097	0.203	0.226	0.1490	0	0.163	0.270	0.266	0.138
	11	0.183	0.237	0.138	0.0594	0.229	0.264	0.126	0.117	0.313
	13	0.091	0.069	0.014	0.0908	0.091	0.008	0.091	0.118	0.038
	15	0.083	0.020	0.069	0.0831	0	0.091	0.084	0.025	0.116
	17	0.141	0.069	0.189	0.04398	0.176	0.156	0.097	0.237	0.036
	19	0.061	0.118	0.100	0.0929	0.136	0.062	0.170	0.013	0.200
	21	0	0	0	0	0	0	0	0	0

(Table I continued on next page.)

Table I (Continued). $(K_{pn}K_{dn})/(K_{p1}K_{d1})$ **for Pitch and Distribution Shown**

	Pitch	18/21	17/21	16/21	15/21	14/21	13/21	12/22	11/21	10/21
Distribution (7) (3-3)										
$K_{p1}K_{d1}$	1	0.926	0.907	0.884	0.855	0.823	0 785	0.741	0 696	0.646
$\frac{K_{pn}K_{dn}}{K_{p1}K_{d1}}$	1	1	1	1	1	1	1	1	1	1
	2	0.036	0.048	0.059	0.070	0.081	0.091	0.101	0.110	0.119
	3	0.520	0.423	0.304	0.161	0	0.175	0.362	0.551	0.748
	4	0.065	0.079	0.087	0.088	0.081	0.067	0.045	0.016	0.018
	5	0.062	0.011	0.044	0.097	0.140	0.167	0.175	0.157	0.115
	6	0 081	0.083	0.068	0.039	0	0.043	0.0815	0.108	0.116
	7	0	0.127	0.226	0.270	0.243	0.147	0	0.166	0.310
	9	0.153	0.321	0.358	0.236	0	0.258	0 426	0.420	0.218
	11	0.150	0.193	0.113	0.049	0.187	0.216	0.103	0 095	0.255
	13	0.017	0.013	0.003	0.017	0.017	0.001	0 017	0.022	0.007
	15	0.081	0.019	0.068	0.081	0	0.088	0.081	0.025	0.113
	17	0	0	0	0	0	0	0	0	0
	19	0.073	0.142	0.120	0.112	0.163	0.074	0.204	0.016	0.240
	21	0	0.255	0	0.270	0	0.294	0	0.332	0
Distribution (7) (6-)										
$K_{p1}K_{d1}$	1	0.934	0.915	0.891	0.862	0.830	0.791	0.747	0.702	0.652
$\frac{K_{pn}K_{dn}}{K_{p1}K_{d1}}$	1	1	1	1	1	1	1	1	1	1
	2	0.036	0.047	0.059	0.070	0.080	0.090	0.100	0.110	0.118
	3	0.568	0.462	0.332	0.176	0	0.192	0.396	0.602	0.816
	4	0.064	0.078	0.086	0.087	0.080	0.066	0.044	0.016	0.018
	5	0.123	0.021	0.087	0.190	0.275	0.328	0.344	0.310	0.227
	6	0.080	0.082	0.066	0.039	0	0.042	0.081	0.107	0.115
	7	0	0.042	0.075	0.089	0.080	0.049	0	0.055	0.102
	9	0.111	0.233	0.259	0.171	0	0.187	0.309	0.304	0.158
	11	0.134	0.173	0.101	0.043	0.167	0.193	0.092	0.085	0.228
	13	0.013	0.010	0.002	0.013	0.013	0.001	0.013	0.017	0.005
	15	0.121	0.028	0.101	0.121	0	0.132	0.122	0.037	0.169
	17	0.088	0.043	0.118	0.027	0.109	0.097	0.060	0.147	0.022
	19	0.030	0.057	0.048	0.045	0.066	0.030	0.082	0.006	0.097
	21	0	0.084	0	0.089	0	0.097	0	0.110	0
Distribution (6′-1)(1′-6)										
$K_{p1}K_{d1}$		0.921	0.902	0.878	0.849	0.818	0.780	0.737	0.692	0.642
$\frac{K_{pn}K_{dn}}{K_{p1}K_{d1}}$	1	1.0	1.0	1.0	1.0	1.0	1.0	1.0	1.0	1.0
	2	0.011	0.013	0.017	0.020	0.023	0.026	0.028	0.032	0.034
	3	0.493	0.402	0.288	0.150	0	0.167	0.343	0.524	0.710
	4	0.036	0.043	0.048	0.048	0.044	0.037	0.024	0.009	0.009
	5	0.067	0.011	0.048	0.105	0.152	0.181	0.189	0.170	0.125
	6	0.131	0.134	0.112	0.064	0	0.069	0.132	0.175	0.189
	7	0	0.039	0.071	0.084	0.076	0.045	0	0.051	0.097
	9	0.024	0.050	0.056	0.036	0	0.040	0.067	0.065	0.034
	11	0.005	0.008	0.005	0.002	0.007	0.009	0.004	0.004	0.011
	13	0.034	0.026	0.005	0.034	0.034	0.003	0.034	0.045	0.014
	15	0.106	0.026	0.089	0.106	0	0.115	0.107	0.033	0.148
	17	0.052	0.026	0.071	0.016	0.066	0.058	0.037	0.088	0.014
	19	0.033	0.063	0.054	0.049	0.073	0.033	0.091	0.006	0.011
	21	0	0.159	0	0.168	0	0.183	0	0.208	0

to equation 27 the coefficients of the Fourier series describing this distribution are

$$C_n=\frac{2}{n\pi}\left[\sin\frac{n\pi P}{2}\right]\left[\frac{2}{bn}\sin\frac{nb}{2}\right]$$
$$=\frac{2}{n\pi}\,k_{pn}(\text{adjustment factor}) \qquad (28)$$

Thus for the higher harmonics the pitch factor may be modified by the "adjustment factor" as an approximate means of compensating for some of the errors in the simpler method of determining the pitch factor.

For the condition that the width, b, of the current-carrying ribbon is one half of the slot pitch, a few values of the aforementioned adjustment factor, (values of $[2/bn]\sin[bn/2]$), are tabulated here:

	Slots Per Pair of Poles		
Harmonic	36	48	66
1	1.0	1.0	1.0
5	0.992	0.995	0.997
11	0.961	0.977	0.988
17	0.91	0.95	0.97
25	0.81	0.89	0.94
35	0.65	0.79	0.89

Since the factors in the preceding tabulation are of interest only where a large proportion of the loss arises from the higher harmonics, and because it is an approximate correction to the usual method of determining pitch factor, it is not included in the tabulations of pitch and distribution factors.

The Distribution Factor k_{dn}

By means of the pitch factor, the value of the nth harmonic magnetomotive force because of the current in any one coil may be determined. By means of the distribution factor, the resultant magnetomotive force of a number of coils is found. Equations are developed in the succeeding paragraphs which cover most of the cases encountered.

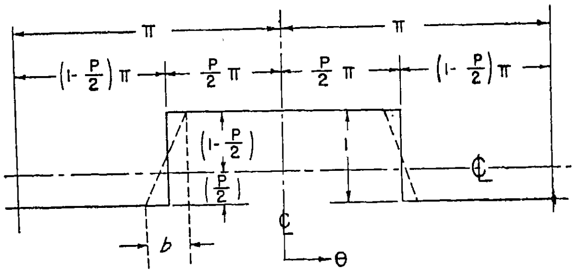

Figure 4. Determination of pitch factor

See Appendix B

In Figure 5 the unit magnetomotive force vectors of each of a group of uniformly spaced coils are shown within the circle which they define.

The radius of the circle, r, may be written in terms of the electrical slot angle, β; the number of the harmonic, n; and the number of coils in the group C.

$$r={}^1/_2\left(\frac{1}{\cos\dfrac{\pi-n\beta}{2}}\right)$$

The length of the resultant vector, divided by the numerical sum of the individual vectors, is the distribution factor

$$k_{dn}=\frac{1}{C}\left(\frac{\cos\dfrac{\pi-cn\beta}{2}}{\cos\dfrac{\pi-n\beta}{2}}\right)=\frac{1}{C}\left(\frac{\sin\dfrac{cn\beta}{2}}{\sin\dfrac{n\beta}{2}}\right) \qquad (29)$$

If the center of the coil group is taken as the point of reference for the group, the resultant vector is either directly in phase or out of phase with the central coil (with an even number of coils the central coil is hypothetical). Because it is sometimes necessary to obtain the resultant of several dissimilar groups of coils, it is necessary to determine the sign of this resultant, which is the sign of equation 29.

It is evident from the foregoing paragraph that in the determination of the net distribution factor for any number of groups of coils connected in series, a graphical solution is readily made. It is also practicable to make an analytical solution as is shown in the following paragraphs.

Consider m groups of coils connected in series, each group having a given number of coils ($C_{\text{I}}, C_{\text{II}}, \ldots C_m$) and a distribution factor ($k_{d\text{I}n}, k_{d\text{II}n}, \ldots k_{dmn}$). The angles, γ, between the center lines of the groups are known; for the harmonic, n, these angles are: $n\gamma_{(\text{I}-\text{II})}$, $n\gamma_{(\text{I}-\text{III})}$, and so forth.

If the angle of the resultant of the first group is considered to be the origin, it may be shown that the net distribution factor is

$$k_{dn}=\frac{1}{c_{\text{I}}+c_{\text{II}}+\ldots+c_m}\times\sqrt{\begin{bmatrix} c_1k_{d\text{I}n}\pm \\ \pm c_2k_{d\text{II}n}\cos n\gamma_{(\text{I}-\text{II})} \\ \cdots\cdots\cdots\cdots \\ \pm c_mk_{dmn}\cos n\gamma_{(\text{I}-m)}\end{bmatrix}^2+\begin{bmatrix} \pm c_{\text{II}}k_{d\text{II}n}\sin n\gamma_{(\text{I}-\text{II})} \\ \cdots\cdots\cdots\cdots \\ \pm c_mk_{dmn}\sin n\gamma_{(\text{I}-m)}\end{bmatrix}^2} \qquad (30)$$

where the plus sign is used for connection in a direction the same as the first group, and the minus sign for connection in the opposite direction. The sign of the distri-

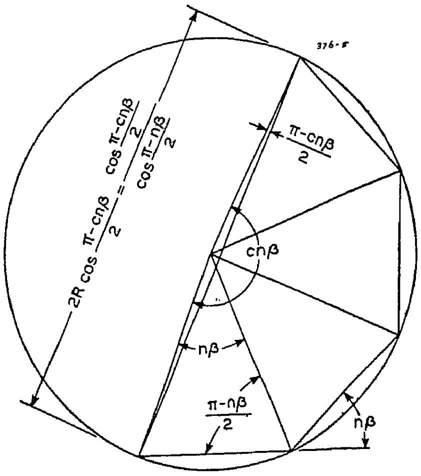
376-F
$2R\cos\frac{\pi-cn\beta}{2}=\frac{\cos\frac{\pi-cn\beta}{2}}{\cos\frac{\pi-n\beta}{2}}$
$\frac{\pi-cn\beta}{2}$
$cn\beta$
$n\beta$
$\frac{\pi-n\beta}{2}$
$n\beta$

Figure 5. Determination of distribution factor

See Appendix B

bution factor for each group as found in the previous section must be carried into equation 30.

For two identical groups equation 30 reduces to.

$k_{dn}=k_{d\mathrm{I}n}\cos {}^1/_2 n\gamma_{(\mathrm{I}-\mathrm{II})}$ for connection in the same direction

$k_{dn}=k_{d\mathrm{I}n}\sin {}^1/_2 n\gamma_{(\mathrm{I}-\mathrm{II})}$ for connection in the opposite direction **(31)**

For any two groups,

$$k_{dn}=\frac{1}{c_\mathrm{I}+c_\mathrm{II}}\sqrt{c_\mathrm{II}{}^2k_{d\mathrm{II}n}{}^2+c_\mathrm{I}{}^2k_{d\mathrm{I}n}{}^2\pm 2c_1k_{d\mathrm{I}n}c_\mathrm{II}k_{d\mathrm{II}n}\cos n\gamma_{(\mathrm{I}-\mathrm{II})}} \qquad \mathbf{(32)}$$

where the sign convention is the same as that stated previously.

Table II A. Magnetomotive-Force Harmonics, Three-Phase Machine

F Is Forward Moving Wave; B Backward

Space Harmonic, n	Current Harmonic, m 0	1	2	3	4	5	6	7
0	BF			BF			BF	
1		F	B		F	B		F
2		B	F		B	F		B
3	BF			BF			BF	
4		F	B		F	B		F
5		B	F		B	F		
6	BF			BF			BF	
7		F	B		F	B		F

Table II B. Magnetomotive-Force Harmonics, Six-Phase Machine

F Is Forward Moving Wave; B Backward

Space Harmonic, n	Current Harmonic, m 0	1	2	3	4	5	6	7
0	BF						BF	
1		F				B		F
2			F		B			
3				BF				
4			B		F			
5		B				F		
6	BF						BF	
7		F				B		F

Table II C. Magnetomotive-Force Harmonics, Two-Phase (90-Degree) Machine

F Is Forward Moving Wave; B Backward

Space Harmonic, n	Current Harmonic, m 0	1	2	3	4	5	6	7
0	BF	0.707BF	0	0.707BF	BF	0.707BF	0	0.707BF
1	0.707BF	F	0 707BF	B	0.707BF	F	0.707BF	B
2	0	0 707BF	BF	0.707BF	0	0.707BF	B	0.707BF
3	0.707BF	B	0.707BF	F	0.707BF	B	0.707BF	F
4	BF	0.707BF	0	0.707BF	BF	0.707BF	0	0.707BF
5	0.707BF	F	0.707BF	B	0.707BF	F	0.707BF	B
6	0	0.707BF	BF	0.707BF	0	0.707BF	BF	0.707BF
7	0.707BF	B	0.707BF	F	0.707BF	B	0.707BF	F

In Table I are given the values of the ratio $(k_{pn}k_{dn})/(k_{p1}k_{d1})$ for several values of pitch for each of several distributions for machines with 42 slots per pair of poles. The tabulation is presented to permit a few generalizations concerning the effect of various winding arrangements on the pole-face loss. The shorthand notation for the distribution is explained by the following examples: (7) is seven coils connected in series in consecutive slots; (7) (6-) is the same as (7) under one pole, with six in series and one slot open under the opposite pole connected in series in the opposite direction to the (7); (1-5-1) represents interspersion of the two end coils of a phase group with the end coils of adjacent phase groups.

The Phase Relationships

Where the currents in each of the phases of a multiphase system are equal and symmetrically displaced with respect to the system, the currents in each of the phases may be represented by

$$i_1=\sum I_m\cos m(\omega t)$$

$$i_2=\sum I_m\cos m\left(\omega t-\frac{2\pi}{Q}\right)$$

$$\cdots\cdots\cdots\cdots\cdots\cdots \qquad \mathbf{(33)}$$

$$i_q=\sum I_m\cos m\left(\omega t-\frac{q-1}{Q}2\pi\right)$$

where i_q is the current in the qth phase, Q is the total number of phases, and I_m is the value of the current of harmonic order, m.

Where these currents flow in the armature of a machine, symmetrically wound with respect to the phases, the magnetomotive force acting on the air gap will be:

$$\mathrm{mmf}_1=\sum_{n=1}^{n=\infty}\sum_{m=1}^{m=\infty}i_1D_n\cos n\theta$$

$$\mathrm{mmf}_2=\sum_{n=0}^{n=\infty}\sum_{m=0}^{m=\infty}\frac{i_2}{i}D_n\cos n\left(\theta-\frac{2\pi}{Q}\right)$$

$$\cdots\cdots\cdots\cdots\cdots\cdots$$

$$\mathrm{mmf}_q=\sum_{n=0}^{n=\infty}\sum_{m=0}^{m=\infty}i_qD_n\cos n\left(\theta-\frac{q-1}{Q}2\pi\right) \qquad \mathbf{(34)}$$

where the subscript, n, indicates the order of the space harmonic, mmf_q is the magnetomotive force produced by the qth phase, and D_n may be defined by

$$D_n \text{ is proportional to } \frac{k_{pn}k_{dn}}{n} \qquad (35)$$

The equations 34 may be written in a more convenient form, as follows:

$$\mathrm{mmf}_1=\sum_{n=1}^{n=\infty}\sum_{m=1}^{m=\infty}\frac{I_mD_n}{2}\times \cos\left[(m\omega t+n\theta)+(m\omega t-n\theta)\right]$$

$$\mathrm{mmf}_2=\sum_{n=1}^{n=\infty}\sum_{m=1}^{m=\infty}\frac{I_mD_n}{2}\times \cos\left(m\omega t-\frac{m2\pi}{Q}+n\theta-\frac{n2\pi}{Q}+\right. \left.\cos\left(m\omega t-\frac{m2\pi}{Q}-n\theta+\frac{n2\pi}{Q}\right)\right.$$

$$\cdots\cdots\cdots\cdots\cdots\cdots$$

$$\mathrm{mmf}_q=\sum_{n=1}^{n=\infty}\sum_{m=1}^{m=\infty}\frac{I_mD_n}{2}\times \cos\left(m\omega t-\frac{q-1}{Q}m2\pi+n\theta-\frac{q-1}{Q}n2\pi\right)+ \cos\left(m\omega t-\frac{q-1}{Q}m2\pi-n\theta+\frac{q-1}{Q}n2\pi\right) \qquad \mathbf{(36)}$$

A study of equation 36 shows that a forward (the same direction as the fundamental) revolving wave of magnetomotive force exists where

$$m-n=0,\ \pm Q,\ \pm 2Q \text{ and so forth} \qquad \mathbf{(37)}$$

and a backward revolving magnetomotive force wave exists when

$$m+n=0,\ \pm 2Q, \text{ and so forth} \qquad \mathbf{(38)}$$

The magnitudes of these revolving waves is

$$(Q/2)I_mD_n \qquad \mathbf{(39)}$$

The following ratio may therefore be written

$$\frac{\text{Magnitude of any mmf wave}}{\text{Magnitude of fundamental mmf wave}}=\frac{I_m}{I_1}\,\frac{1}{n}\,\frac{k_{pn}k_{dn}}{k_{d1}k_{d1}} \qquad \mathbf{(40)}$$

If the rotor is revolving at the speed of the fundamental, the speed of the traveling wave with respect to the rotor is

$$\left(\frac{m}{n}\pm 1\right)\omega \text{ radians per second} \qquad \mathbf{(41)}$$

where the plus sign is used with a backward revolving wave, and the minus sign is used with a forward revolving wave. The forward direction is the direction of rotor rotation.

The frequency of the currents generated in the rotor surface by these traveling

Tabl II D. Magnetomotive-Force Harmonics, Four-Phase Machine

F Is Forward Moving Wave; B Backward

Space Harmonic, n	Current Harmonic, m 0	1	2	3	4	5	6	7
0	BF				BF			
1		F		B		F		B
2			BF				BF	
3		B		F		B		F
4	BF				BF			
5		F		B		F		B
6			BF				BF	
7		B		F		B		F

magnetomotive force waves may be written,

$$\frac{\omega}{2\pi}\left(\frac{m}{n} \pm 1\right)n \qquad (42)$$

with the same sign covention as in equation 41.

In Tables II are shown the magnetomotive force harmonics which can exist in three-, six-, two- (90°), and four-phase symmetrically wound machines. The two-phase machine does not fulfill the general conditions established for the preceding equations, and special rules must be applied. The exceptional case of the four-phase machine II-D is included for reference in reviewing the tabulation for the two-phase winding. Similarly, the six-phase machine can be considered to represent a three-phase machine with six symmetrically wound phase groups.

In Tables II the letter *F* indicates a magnetomotive force wave revolving forward, in the same direction as the fundamental; *B*, a backward wave. The coexistence of both is equivalent to a standing pulsating wave. All harmonics have a strength of

$$\frac{I_m}{I}\,\frac{k_{pn}k_{dn}}{k_{p1}k_{d1}}\,\frac{1}{n}$$

times the fundamental except where stated in the case of the two-phase machine.

In the cases usually encountered, the armature currents are substantially of single frequency, and only that part of the tabulations is applicable which refers to fundamental frequency currents. The equations for pole-face loss as written in Appendix A are then directly applicable.

Where harmonic currents are sufficiently large to cause appreciable loss, the loss equations are applicable by multiplying them by $(I_m/I_1)^2$ for those portions of the loss caused by the harmonic currents. In the case of the two-phase machines, a factor $(0.707)^2$ must also be included in the loss equations where it applies.

Appendix C

Sample Calculations and Tests

When the equations which have been written are used to calculate losses in actual machines, it is necessary to make allowances for departure from the idealized case used in the development of the equations.

The length of the air gap for the determination of pole-face losses is chosen in the calculations which follow as that used by designers in the determination of the relation between fundamental air gap magnetomotive force and flux density.

The slots in the rotor surface affect the permeability as used in the equations. The eddy current losses occur only in the skin of the rotor (except for very low frequencies); the magnetic fluxes which coexist with these eddy currents are directed primarily around the periphery of the machine. In the regions near the pole centers they lie wholly in solid iron; in the region of the slots they encounter wedges, which, if they are magnetic, are usually saturated by the main field current. For this reason the effective permeability of the rotor to these stray fluxes should probably never be assumed to exceed a value greater than several hundred times that of air.

As is evident in the examples considered below, where high pole-face losses occur, the stray fields themselves tend to saturate the rotor surface. Because the density of the eddy currents decreases rapidly and exponentially with depth below the rotor surface, a nonuniform permeability exists. When losses are caused by simultaneous action of magnetomotive forces of more than one frequency, the eddy currents of higher frequency, because they lie closer to the surface, are associated with a lower permeability. The device used below to treat, or rather to obviate this complex situation is to assume a permeability (if it is less than about 400) such that the flux density at the surface is 200,000 to 300,000 lines per square inch, about twice the saturation point of common steels, and to assume that losses attributable to the different harmonics can be calculated separately and added directly.

Zero-Sequence Connection

In this test a machine was operated at 1,800 and 3,600 revolutions per minute and with direct current applied to the armature winding connected open delta. Thus only the odd triple harmonics of armature magnetomotive force appear. From equations 36 and 23, the following relation is written.

$$I_{on} = \frac{A_{(\text{rated})}P}{D}\,\frac{4}{\pi}\,\frac{k_{pn}k_{dn}}{k_{p1}k_{d1}}\,\frac{I_{dc}\sqrt{2}}{I_{\text{rms(rated)}}} \qquad (43)$$

Data concerning the machine follow:

Inside stator diameter	20 inches
Active length	30 inches
Air gap	0.375 inch and increased to 0.500 inch
Effective gap	0.437 and 0.562
Rated rpm	3,600
Rated armature reaction, *A*	9,000 ampere turns per pole
Rated stator winding current	451 amperes
Phases, 3	36 coils in 36 slots
Pitch	11/18

$\frac{k_{pn}k_{dn}}{k_{p1}k_{d1}}$ for 3rd, 9th, 15th, and so forth, harmonics $= 0.213$

For the calculation of losses gap diameter, *D*, is taken as 19.5 inches; $\rho = 20 \times 10^{-6}$ ohm-centimeters.

For convenience in calculation, the following equations may be written for the specific problem from equations 43 and 19:

$$\text{Kw loss} = 7.00\left(\frac{I_{on}}{100}\right)^2\left(\frac{\text{rpm}}{3{,}600}\right)\times \sum^{3,\,9,\,15,\ \text{etc.}} R_n \qquad (44)$$

$$I_{on}\ (\text{amperes per centimeter}) = 0.308 I_{dc}$$

The factor I_{on} is the same for all triple *n* harmonics in this special case, which simplifies the computation.

The results are shown in Table III. In the calculation *R* is determined both from Figure 2 and by the simpler method of Figure 3. It should be noted that calculations based on the simpler method of determining *R* always show the highest value, as

Table III. Sample Calculation of Pole-Face Loss, Open-Delta Connection; See Appendix C

Method of Calculation	I_{dc} (Amperes)	Gap (Inches)	Revolutions Per Minute	I_{03} (Amperes Per Centimeter)	$\frac{G_{eff}}{l_3}$	μ	$\sqrt{\frac{f_3 l_3^2 10^{-9}}{\mu\rho}}$	R_3	R_9	R_{15}	S_3	β_3 Lines (Square Centimeters)	Calculated Pole-Face Loss (Kilowatts)	Test Load Loss (Kilowatts)
Figure 2	250	0.375	3,600	77	0.021	100	0.49	0.75	0.44	0.21	1.1	8,500	5.8	9.7
Figure 2	250	0.375	3,600	77	0.021	400	0.25	1.1	0.44	0.15	1.0	31,000	7.0	9.7
Figure 3	250	0.375	3,600	77	0.021			1.6	0.45	0.22			9.4	9.7
Figure 2	350	0.375	3,600	108	0.021	100	0.49	0.75	0.44	0.21	1.1	11,900	11.5	19.6
Figure 2	350	0.375	3,600	108	0.021	200	0.35	0.95	0.45	0.15	1.0		12.7	19.6
Figure 2	350	0.375	3,600	108	0.021	400	0.25	1.1	0.44	0.15	1.0	43,300	13.8	19.6
Figure 3	350	0.375	3,600	108	0.021			1.6	0.45	0.22			18.6	19.6
Figure 2	350	0.375	1,800	108	0.021	200	0.25	1.2	0.45	0.12	1.0	21,600	7.5	10.3
Figure 2	350	0.375	1,800	108	0.021	400	0.17	1.35	0.45	0.10	0.9	39,000	7.8	10.3
Figure 3	350	0.375	1,800	108	0.021			1.6	0.45	0.22			9.3	10.3
Figure 2	350	0.500	3,600	108	0.027	400	0.25	1.1	0.27	0.08	0.90	39,000	11.9	12.8
Figure 3	350	0.500	3,600	108	0.027			1.2	0.33	0.15			14.1	12.8

Table IV. Sample Calculation of Pole-Face Loss, Line-to-Line Short Circuit; See Appendix C

Revolutions Per Minute	Amperes	μ	$\sqrt{\frac{f1^2 10^{-9}}{\mu\rho}}$	R	S	Calculated Kilowatts	Machine Lines Per Square Centimeters	Test Load Loss (Kilowatts)
3,600	750	50	2.04	0.24	1.25	4.1	4,900	16.6
3,600	750	200	1.02	0.47	1.25	8.0	19,500	16.6
3,600	750	400	0.725	0.65	1.25	11.0	39,000	16.6
3,600	1,250	50	2.04	0.24	1.25	11.4	8,200	35.5
3,600	1,250	200	1.02	0.47	1.25	22.0	33,000	35.5
3,600	1,250	400	0.725	0.65	1.25	30.5	65,000	35.5
2,000	1,250	50	1.52	0.31	1.25	8.2	8,200	23.5
2,000	1,250	200	0.75	0.63	1.25	15.6	33,000	23.5
2,000	1,250	400	0.54	0.83	1.20	22.0	64,000	23.5

Table V. Sample Calculation of Pole-Face Loss, Normal Machine; See Appendix C

n	K_p	K_d	$\left(\frac{k_{pn}k_{dn}}{k_{p1}k_{d1}}\right)^2$	$n\frac{q}{D}\frac{P}{2}$	R (Figure 3)	$\frac{f_{nr}}{nf_1}$	$\frac{f_{nr}}{nf_1}\left(\frac{k_{pn}k_{dn}}{k_{p1}k_{d1}}\right)^2 R$
1	0.866	0.958	1.0	0.037	—	—	—
2	0.866	0.077	0.0065	0.074	0.65	3/2	0.0063
4	0.866	0.077	0.0065	0.148	0.56	3/4	0.0027
5	0.866	0.264	0.0760	0.185	0.51	6/5	0.0465
7	0.866	0.077	0.0065	0.259	0.33	6/7	0.0018
11	0.866	0.160	0.028	0.407	0.165	12/11	0.0050
13	0.866	0.013	—	0.481	0.120	12/13	—
17	0.866	0.105	0.011	0.628	0.066	18/17	0.0008
19	0.866	0.063	0.0043	0.702	0.049	18/19	—
						Total	0.0631

$$\text{Loss} = \text{total} \times \left(\frac{A}{10{,}000}\right)^2 \frac{L}{10}\,\frac{P}{2}\,fo = 0.0631 \times 740 = 46 \text{ kw}$$

they should, and that the simpler method is in reasonable agreement with the more complex method for harmonics of higher order than the third and lower order than the fifteenth.

The test loss as shown is the total loss of the machine on short circuit, less the windage and friction and less the total copper loss as calculated. It therefore includes stray losses other than pole-face loss, and should be consistently larger than the calculated pole-face loss.

Single-Phase Short Circuit

When a machine is operated on single-phase short circuit, the armature reaction may be considered to have two fundamental components, one revolving in the same direction as the rotor and stationary with respect to the rotor, the other revolving in a direction opposite to the rotor and inducing in its surface eddy currents of twice armature current frequency. This second component produces the pole-face loss, the subject of calculation below; because the fundamental of armature magnetomotive force is so large compared with the harmonics, the losses attributable to these harmonic magnetomotive forces are neglected.

From equations 23 and a consideration of the development leading to equation 36, for a line-to-line short circuit

$$I_{01} = A_{(\text{rated})}\,\frac{P}{D}\,\frac{4}{\pi}\left(\frac{I\text{ actual}}{I\text{ rated}}\,\frac{1}{\sqrt{3}}\right) \qquad (45)$$

Pertinent data concerning the machine tested follow:

Inside stator diameter......24 inches
Active length..............$37^1/_2$ inches
Air gap...................0.687 inch; effective gap, 0.785 per cent
Rated rpm................3,600
Rated armature reaction, A . 10,140 ampere turns per pole
Rated stator winding current 2,170 amperes
Three-phase line-to-line short circuit on wye connection
For calculations, diameter = 23.25 inches, $\rho = 20 \times 10^{-6}$ ohm-centimeters

From equations 45 and 14 the following equations may be written for calculation

$$\text{Kw loss} = 145\left(\frac{I_{rms}}{2{,}170}\right)^2 R \qquad (46)$$

Maximum flux lines per square centimeter in

$$\text{rotor surface} = 238\left(\frac{I_{rms}}{2{,}180}\right) S\mu$$

The simpler method of Figure 3 for determining R is not applicable; both R and S are determined from Figure 2.

The results of the calculations and tests are presented in Table IV. Here, again, the test loss is the so-called load loss less the stray loss in the copper; it therefore includes stray losses in addition to pole-face loss. Under the assumption, discussed elsewhere, that the permeability should not exceed a value which permits calculated flux densities in excess of about 250,000 lines per square inch (35,000 per square centimeter) in the skin of the rotor the calculations are seen to be consistent at about two thirds the value of the test loss. This difference may indicate that the method requires an empirical factor in the equations or that the other stray losses are large. In either case, it is gratifying to note that the results are of the correct order of magnitude, even for cases where the departures from the fundamental assumptions are great.

Normal Machine

For machines of normal design, the calculation is most readily made if equation 24 is written in the form

$$Kw = \sum^{n}\left[\left(\frac{A}{10{,}000}\right)^2 \frac{L}{10}\,\frac{P}{2} f_1\right] \times \left[\left(\frac{f_{nr}}{nf_1}\right)\left(\frac{k_{pn}k_{dn}}{k_{p1}k_{d1}}\right)^2 R\right] \qquad (47)$$

where

f_1 = fundamental armature frequency
f_{nr} = time order of harmonic of current generated in rotor
R—from Figure 3

The items in the second bracket are those which vary with the harmonic.

For the machine under study:

A = 14,330 ampere turns per pole
Pitch = 14/21
Distribution, (7) (6-) one coil omitted at end of one group
Revolutions per minute, 3,600; air gap $^{15}/_{16}$ inch (mechanical); length 60 inches; rotor diameter, $27^1/_8$ inches; 2 poles.
The calculation of loss is made in Table V.

From the tabulation it is apparent that the major part of the loss is caused by the fifth harmonic and that the loss attributable to the very low order harmonics is small. Since the simpler method of Figure 3 is sufficiently accurate for harmonics greater than the third and less than the fifteenth, no attempt for greater accuracy by use of the method of Figure 2 would be fruitful.

From a perusal of Table I the reader will note that there is good possibility of reducing the pole-face loss by changing the armature winding. Calculation shows that a distribution (7) (3-3) would reduce the loss by about 30 per cent. An even greater reduction is possible by using a (6-) (6-) distribution (and a longer pitch—to maintain flux density substantially constant).

The tests on this machine (7) (6-) distribution, showed a total load loss of 45 kw. It has been found that usually the pole-face loss calculation is about 75 per cent of the tested load loss. That the calculated pole-face loss is so nearly equal to the total load loss may be attributed to testing and calculation errors and in part to a stator end structure specially designed for low load loss in that region.

References

1. FRACTIONAL-SLOT AND DEAD-COIL WINDINGS—I, Michel G. Malti, Fritz Herzog. AIEE TRANSACTIONS, volume 59, 1940, pages 782–94.

2. SYNCHRONOUS MACHINES WITH SOLID CYLINDRICAL ROTOR, C. Concordia, H. Poritsky. AIEE TRANSACTIONS, volume 56, 1937, January section. pages 49–58.

3. ADDITIONAL LOSSES OF SYNCHRONOUS MACHINES, C. M. Laffoon, J. F. Calvert. AIEE TRANSACTIONS, volume 46, 1927, pages 84–96.

4. AMPLITUDES OF MAGNETOMOTIVE FORCE HARMONICS FOR FRACTIONAL SLOT WINDINGS OF THREE-PHASE MACHINES. Iowa Engineering Experiment Station, bulletin 142, 1939.

5. THEORY OF ALTERNATING-CURRENT MACHINERY (book) A. S. Langsdorf. 1937, pages 321–50.

Application of Carrier to Power Lines

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DURING the 20 years or so that have elapsed since power-line carrier was first introduced on a commercial basis, enormous advances have been made in the communication arts. In the somewhat specialized branch of power-line carrier, advances in technique and apparatus design have not been as highly publicized as those in some other branches of the art; nevertheless, they have kept pace with the field, with the result that power-line carrier is today one of the most useful and versatile tools available to the power-utility engineer. There are now over 1,300 terminals of power-line carrier in operation in the United States. These terminals provide channels for telephone circuits, pilot relaying, telemetering, load-control, supervisory, and other remote-control functions over transmission lines of all voltages, totaling over 40,000 channel-miles. The last several years have seen a rapid increase in the number of terminals and in channel-miles, and there is every indication that this rate of growth will continue to increase for some time. With very few exceptions, every new transmission line of any importance is now engineered to include power-line carrier for one or more functions. Many large integrated systems[1] have already made such extensive use of carrier circuits that the problem of channel space and frequency assignment is rapidly becoming of major importance.

A considerable portion of the more recent increase in the use of power-line carrier is directly connected with the increased load the war production has placed on the power industry. High-speed protection, obtainable on long lines only through the use of carrier pilot relaying, increases the stability limit of transmission circuits so as to permit increased loading of existing lines. This factor, alone, provides large savings in copper and other transmission-line materials which, otherwise, would be required by new construction or by the use of double-circuit lines with slower relaying. In addition, the reliability and convenience of carrier telephone circuits for dispatching permit existing generating and substation equipment to be operated nearer maximum load and efficiency conditions. The increase in the number of system interconnections to pool generation and transmission facilities for new or increased war production loads has also resulted in a large increase in the use of carrier channels for telemetering and load control.

This growth in the use of power-line carrier has confronted the power-utility engineer with many new problems. To guide him in their solution, it is desirable to make available much of the application information which has been accumulated by those who have concentrated on this work since the initial installation of power-line carrier in 1921.

In answer to the growing demand for application information, it is the principal purpose of this paper to present a description of methods successfully used in calculating transmission losses and applying power-line-carrier equipment. These methods are largely based on experience accumulated over a period of some 20 years in applying carrier to all types of power-transmission lines. They are supported by a considerable amount of field and laboratory data and have been corrected and revised from time to time as new data and experience have indicated. In general, they are somewhat on the conservative side. However, the factors of safety incorporated have not burdened the equipment design, and, in numerous instances, have proved to be essential in maintaining a high degree of reliability under adverse conditions.

Since the behavior of high-frequency currents, when applied to power lines, is sufficiently complex and the difficulties of obtaining quantitative data are sufficiently great, we find that, even at this late date, the process of application is still more of an art than the exact science many would expect it to be. It is not intended, therefore, that the material presented herein be interpreted as the complete and final word on the subject. This paper is intended more as a guide for the power-utility engineer in the preliminary work of laying out carrier channels on a power-transmission system.

For the sake of simplicity, the subject matter is confined entirely to the problems of establishing the carrier circuits; the choice and application of the terminal equipment using these circuits is not covered.

The Problem

Basically, the problem of applying carrier to power lines is one of utilizing circuits that have been designed for the reliable and efficient transmission of power in terms of kilowatts, kilovolts, and hundreds of amperes at very low frequencies and adapting them for the transmission of power in terms of watts, volts, and milliamperes at frequencies of 50 kilocycles or higher. More specifically, for a given carrier channel, the problem resolves itself into one of establishing and maintaining a carrier-frequency path or circuit between two or more transmitting and receiving stations within certain limits of attenuation or transmission loss. The fundamental requirement imposed by the power system is that the means used to adapt the circuits for carrier channels shall not in any way interfere with the primary function of power transmission. At the same time, to attain the desired degree of reliability, the carrier channels established should be free from interference or interruptions caused by switching or normal operation of the power system and transient disturbances attributable to lightning or other causes. It is also essential that the carrier circuits shall be safe; that is, they should be adequately insulated and protected against normal and abnormal voltages and currents so as to impose no hazard to connected apparatus or to personnel using and maintaining the equipment.

One of the most important factors in the problem is the wide differential between power frequencies and frequencies normally used for the carrier circuits. Circuit elements and power apparatus which offer very low impedance paths to power frequencies may appear as very high impedances at carrier frequencies, and high impedances or open circuits for power frequencies may appear as low impedances at carrier frequencies. This phenomenon is largely responsible for the "gap-jumping" myth that has attached itself to power-line carrier. There are numerous instances of the carrier channel being maintained through what appeared to be gaps or breaks in the line, but a painstaking analysis of such cases would doubtless reveal a carrier-frequency path of some sort. Through this potentiality, carrier has rendered invaluable service in many serious emergencies, but this highly desirable faculty is unpredictable and can hardly be given practical treatment in application engineering.

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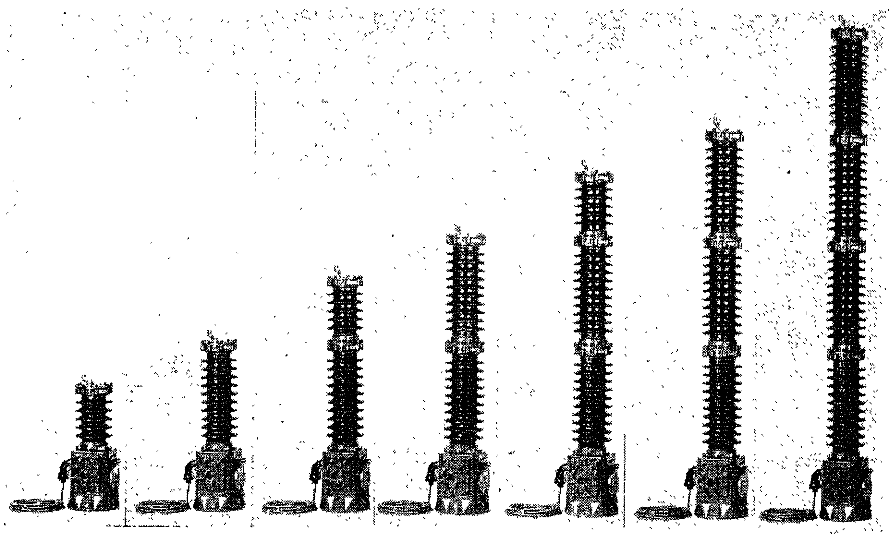


Figure 1. Carrier-current coupling capacitors, type CW

Typical base-mounted assemblies for 34.5 kv, 69 kv, 115 kv, 138 kv, 161 kv, 230 kv, and 287 kv, left to right

In laying out a carrier circuit, to obtain the reliability usually required, it is essential that all obstructions or possible breaks in the carrier path between the channel terminals be removed by definite provision of alternate paths or by-passes for the carrier frequency. Intervening transformers, autotransformers, current-limiting reactors, as well as circuit breakers or disconnect switches that may be opened, usually require by-passing. Low-impedance shunts at the carrier frequency such as some spur lines or parallel branches, cable circuits, and grounds applied when the circuit is de-energized for repairs, usually require trapping.

Another important point to keep in mind is that transmission at carrier frequencies along a transmission line, in common with transmission at lower frequencies, requires a complete circuit; that is, in terms of the simplest concept, there must be a "return path" as well as a "forward path." It is also just as important that this return path have low carrier-frequency loss as it is for the forward path. If one conductor of a transmission line is selected for one side of the circuit or the forward path, the other side of the circuit or the return path is usually provided by one or more of the other conductors, the overhead ground wire, the earth itself, or a combination of these. The selection of this return path and the provision made for its continuity may have considerable bearing on the efficiency of the carrier circuit. The frequent suggestion for the use of oil pipe lines or other buried pipe lines as carrier circuits is an illustration of neglecting this return-path requirement.

Table I. Standard Coupling-Capacitor Assemblies

Standard Capacitor Assemblies		Withstand Test Voltages (Based on Proposed AIEE Standard 31)		
			Low Frequency	
Maximum Rated Apparatus Voltage (Kilovolts)	Capacitance (Microfarads)	Impulse (Kilovolts)	Dry, 1 Minute (Kilovolts)	Wet, 10 Seconds (Kilovolts)
15	0.006	110	50	45
25	0.006	150	65	60
34.5	0.006	200	85	80
46	0.003	250	110	100
69	0.003	350	165	145
92	0.002	450	215	190
115	0.002	550	265	230
138	0.0015	650	320	275
161	0.0012	750	370	315
196	0.001	900	450	385
230	0.001	1,050	525	445
287	0.00075	1,300	655	555
345	0.0006	1,550	785	665

Fortunately, the devices or tools available and actually used in establishing carrier circuits on power lines are relatively simple and few in number. The physical and economic restrictions imposed by a high-voltage power system limit the devices that can be inserted directly in the power line to essentially only two: the coupling capacitor and the line trap. These two, together with line-tuning apparatus associated with the low-voltage side of the coupling capacitor, are the basic tools used. In order to apply these tools most effectively, it is important that their functions, characteristics, and limitations be thoroughly understood.

Coupling Capacitors

In some of the early concepts, power-line carrier was considered as a special case of radio transmission in which the power line was caused to guide the radio waves by locating the antennas associated with the transmitting and receiving equipment as close to the line as possible. The antennas so located were thought of as "coupling" the equipment to the line and were thus called "coupling wires." A great deal of experimental work was done in determining the most effective location and arrangement of these coupling wires, and from this work it soon became apparent that the capacitance between the coupling wires and the line conductors was the most important factor in obtaining the desired end. However, the inefficiency, hazard, and inconvenience of the coupling wire soon led to the development of a high-voltage capacitor for obtaining this capacitance more directly and efficiently, and it naturally followed that this device was called a "coupling capacitor."

After passing through numerous stages in development, covering a considerable range in dielectric material and physical shape, the coupling capacitor has attained the status of a standardized and highly dependable piece of equipment, universally accepted as the standard means for connecting carrier circuits to high-voltage lines. In its modern form, as illustrated by Figure 1, it consists of an assembly of one or more porcelain-shell oil-filled paper-dielectric capacitor units together with a base unit containing a grounding switch and protective equipment. The individual capacitor units are co-ordinated as to capacitance and voltage rating so that they may be connected in series to form assemblies for any standard circuit voltage. Table I shows the capacitances available for various standard apparatus voltage ratings together with test voltages which these assemblies will withstand.

The inclusion of the protective equipment in the capacitor base is now standard practice. This protective equipment usually consists of a gap and a power-frequency drainage coil. The drainage coil ordinarily consists of an inductor connecting the low-voltage side of the capacitor to ground. This coil which has an impedance of over 50,000 ohms at carrier frequencies and less than 100 ohms at power frequencies, serves to provide a path for the power-frequency current of 50 milliamperes or so through the capacitor to ground and thus maintain the lead to the carrier equipment at a normal power-frequency voltage of less than five volts. The gap, which is connected across the drainage coil, limits the voltage which can be built up on the carrier lead during transient voltage disturbances.

Frequently, the coupling capacitor is

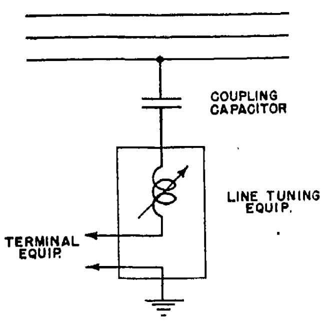


Figure 2. Simple single-frequency tuning

used with a capacitor potential device to provide potential for instruments and relays. The potential device usually forms the base for the capacitor assembly in these cases, replacing the capacitor base unit. A choke coil, having impedance characteristics similar to the drainage coil, is used between the capacitor and potential device to maintain a high shunt impedance for carrier frequencies on the carrier lead. The use of the potential device also inserts an auxiliary series capacitor in the carrier lead. However, since the capacitance of this auxiliary capacitor is large compared to that of the coupling capacitor, the slight reduction in total capacitance resulting from the use of the potential device can usually be neglected.

From the standpoint of the power-transmission system, it is essential that the insulation level of the capacitor assembly, in terms of impulse and wet and dry flashover withstand voltages, be co-ordinated with that of associated power-line apparatus. From the standpoint of the carrier circuit, it is desirable that the capacitance be as large as possible, consistent with an economic design, preferably at least 0.001 microfarad. Also, from the carrier-circuit standpoint, it is essential that the carrier lead brought out from the base of the capacitor assembly be well insulated so as to maintain low capacitance and high leakage resistance to ground between the capacitor and the line-tuning equipment. The variation in capacitance with temperature should be sufficiently small as to have negligible effect on the carrier-frequency circuit.

Line-Tuning Equipment

Associated with each coupling capacitor in its function of connecting carrier circuits into the power lines, certain auxiliary apparatus, generally known as line-tuning equipment, is usually required. This equipment, located in the circuit betwen the coupling capacitor and the carrier-terminal equipment, performs the primary function of tuning or resonating with the capacitive reactance of the coupling capacitor to provide a low-loss path for one or more carrier frequencies or bands of carrier frequencies between the terminal equipment and the power line. The combination of line-tuning equipment and coupling capacitor may be thought of as forming a filter of the band-pass type, passing or offering a low-loss path for the desired carrier frequencies or channels and rejecting or offering a high-loss path for the power frequency and undesired frequencies in the carrier band.

Line-tuning equipment may be very simple or fairly complex, depending on the number of carrier frequencies or channels to be passed through the coupling capacitor. The question of the number of frequencies or channels that can be handled through a single coupling capacitor is primarily one of balancing the cost of additional capacitors against the cost and the inconvenience of additional complexity in the line-tuning equipment. The most commonly used line-tuning equipment is available in standard units, having various arrangements of variable inductors and capacitors that can be combined to form series and parallel resonant circuits for one or more channels in the normal range of 50 to 150 kilocycles.

Single-Frequency Line-Tuning Equipment

In its simplest form, for a single frequency or channel, the line-tuning equipment may consist of a single variable inductor connected in series with the coupling capacitor to form a series-resonant circuit for the carrier frequency used. Such a circuit is shown schematically in Figure 2, where, for simplicity, a ground-return circuit is assumed. Since the portion of the circuit between the capacitor and the line-tuning inductor operates at highest carrier-frequency impedance (to ground), it is highly desirable that shunt capacitance or leakage to ground in this lead be kept at a minimum for maximum efficiency. For this reason it is usually desirable to locate the line-tuning equipment as close to the coupling capacitor as possible, in order to keep the length of this lead-in conductor to a minimum. However, by using a well-insulated conductor, supported by a minimum number of insulators, lead-in lengths of up to 100 feet or so may be used without excessive losses in this portion of the circuit.

Table II. Recommended Maximum Concentric-Cable Lengths When Outdoor Line Tuning Is Omitted

Size of Coupling Capacitor (Microfarads)	Recommended Maximum Cable Lengths in Feet From Coupling Capacitor to Terminal Equipment (Kilocycles)					
	1-Decibel Attenuation			2-Decibel Attenuation		
	50	85	150	50	85	150
Impedance Match Transformer Not Used						
0.00075	40	60	75	90	120	140
0.001	70	100	110	140	160	190
0.0012	110	120	130	200	200	210
0.0015	120	120	160	230	240	250
0.002	210	220	230	340	340	340
0.003	320	320	310	500	480	420
0.006	500	480	370	800	700	550
Impedance Match Transformer Used						
0.00075	150	180	200	300	350	410
0.001	200	230	250	390	430	600
0.0012	240	270	300	430	500	750
0.0015	260	300	340	500	600	800
0.002	360	390	460	680	800	1,000
0.003	450	450	475	900	1,000	1,000
0.006	800	800	800	1,000	1,000	1,000

For some years it has been standard practice to mount some types of carrier-terminal equipment outdoors near the coupling capacitor; in such cases the line-tuning equipment is included in the same cabinet with the terminal equipment. While this arrangement eliminates the losses in the circuit between the line-tuning and terminal equipments, and, in some cases, simplifies the installation somewhat, general experience has shown these advantages to be outweighed heavily by the inconvenience of maintaining the terminal equipment under extreme weather conditions. More recent practice is to install all terminal equipment indoors and to carry the circuit in from the line-tuning equipment through a special low-loss concentric cable. The transmission losses in this special cable, as will be noted in Table II, are sufficiently low as to permit this portion of the circuit to be carried several thousand feet without excessive loss. Since the concentric cable has a characteristic impedance of approximately 70 ohms and the line-tuning coupling-capacitor circuit an impedance in the order of 400 to 800 ohms, an impedance-matching transformer is usually used between the cable

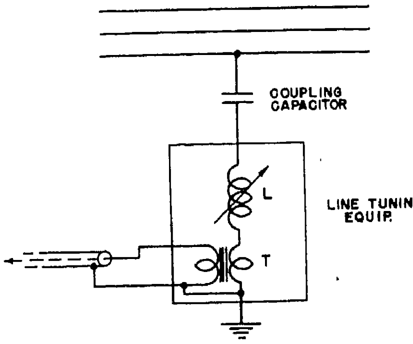


Figure 3. Single-frequency tuning with impedance-matching transformers

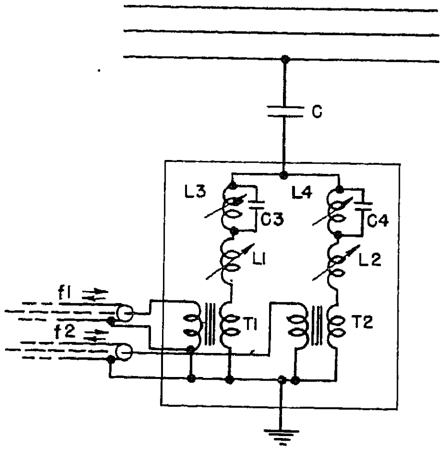


Figure 4. Two-frequency tuning of single coupling capacitor with separate lead-in cables

and the line-tuning equipment, as shown in Figure 3.

Multifrequency Line-Tuning Equipment

When a single coupling capacitor is to be used for two separate carrier channels, a commonly used line-tuning circuit is that shown in Figure 4. In this arrangement, the coupling capacitor is connected to two branch circuits, each having a parallel resonant, or "trap" circuit, a series tuning element, and an impedance-matching transformer. In the branch which is tuned to pass frequency $f1$, the trap formed by $L3$-$C3$ is tuned for parallel resonance at frequency $f2$ so as to offer a high impedance for $f2$. The series tuning element $L1$ is tuned for series resonance at $f1$ with the coupling capacitor C and the reactance which the trap $L3$-$C3$ presents at $f1$. Similarly, the other branch is tuned to pass frequency $f2$ with $L4$-$C4$ shunt tuned to $f1$.

By adding a third branch and an additional shunt-tuned trap in each branch, a single coupling capacitor can be used in common for three separate channels as shown in Figure 5. In this arrangement, $L6$-$C6$ and $L9$-$C9$ are tuned to $f1$, $L3$-$C3$ and $L8$-$C8$ are tuned to $f2$, and $L2$-$C2$ and $L5$-$C5$ are tuned to $f3$. The series elements $L1$, $L4$, and $L7$ may be simple variable inductors as indicated, or they may include additional series capacitors, depending on the frequencies involved, the residual series reactance of the trap circuits, and the capacitance of the coupling capacitor.

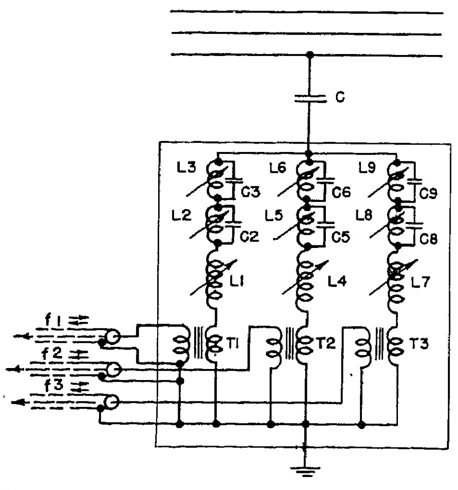


Figure 5. Three-frequency tuning of single coupling capacitor with separate lead-in cables

Because of the excessive losses introduced into each channel and the difficulty of adjustment in the field to obtain satisfactory band-pass characteristics for each channel, it has not been considered practical to extend the arrangements shown in Figures 4 and 5 beyond a maximum of three channels per coupling capacitor. When additional channels are to be coupled to the same line conductor, additional coupling capacitors are generally used.

Frequently, when tuned circuits or filters are included in the terminal equipment for separating the channels of a multichannel system, it is desirable to carry the several channels between the line-tuning equipment and the terminal equipment over a single concentric cable. A single coupling capacitor can be tuned to pass two frequencies through a single cable by using the line-tuning arrangement shown in Figure 6. In this circuit, the inductor $L1$ and the coupling capacitor C are tuned for series resonance at the upper frequency $F1$. $L2$ and $C2$ are also tuned for series resonance at $F1$. By means of the inductor $L3$, the entire circuit can be adjusted for series resonance at the lower frequency $F2$. While this arrangement can be extended to handle as many as three frequencies with standard line-tuning units, here again it is common practice to use additional coupling capacitors if more than three channels are involved.

When the several frequencies of a multichannel system have been selected definitely and are known in advance, it may be advantageous to use conventional band-pass filter circuits such as shown in Figure 7. To use this type of circuit it is necessary that the several channels be grouped together in one part of the frequency spectrum. The capacitance available in the coupling capacitor is an important factor in the maximum band width provided by this type of circuit, particularly at the lower end of the frequency spectrum, and in some cases two coupling capacitors in parallel may be required. While commercial designs of this type of line-tuning equipment are not generally available as yet, the rapid increase in the number of channels being applied indicates that their general use in the near future can be predicted safely.

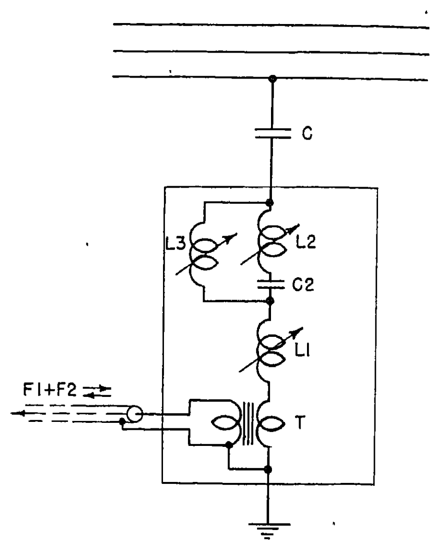


Figure 6. Two-frequency tuning of single capacitor with common lead-in cable

Omission of Outdoor Line-Tuning Equipment

In the interest of maximum theoretical efficiency, it is usually assumed that line-tuning circuits, such as have been described thus far, are adjusted and operated so that the reactance of the coupling capacitor and the reactive components presented by the transmission line itself are largely compensated or "tuned out," and that the concentric cable is terminated thus in a resistive load approximately equal to its characteristic impedance. While this maximum theoretical efficiency is generally desirable, there are many instances in which it is not essential. Carrier circuits, having total transmission losses well within the maximum range of the terminal equipment used, can frequently accommodate a considerable amount of loss in the line-tuning and lead-in circuit with no appreciable decrease in reliability. Some

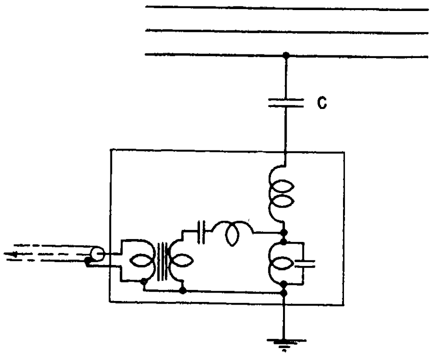


Figure 7. Band-pass tuning of single coupling capacitor

consideration may therefore be given to arrangements that strive for something appreciably less than maximum efficiency.

If the concentric-cable lead-in is brought into the base of the coupling capacitor and connected directly to the low side of the capacitor, without benefit of a line-tuning unit, as shown in Figure 8, there will be a considerable mismatch in impedance at the junction of the cable and capacitor. The resulting reflections or standing waves will cause the losses in the cable to exceed considerably those shown in Figure 22 for ideal termination conditions. However, under favorable conditions and depending on the size of the coupling capacitor and the carrier frequency involved, the cable may be several hundred feet in length and still not add over two decibels loss to the circuit. Compared with a total allowable attenuation varying from 30 to 60 or more decibels for various transmitter-receiver combinations, this loss is small and frequently can be allowed. If an impedance-matching transformer is added to this circuit, as shown in Figure 9, to step down the impedance of the capacitor and the line to a value numerically approximating the cable-characteristic impedance, the effect of the mismatch will be considerably reduced and a greater length of cable can be used with the same loss. Table II shows the lengths of cable that may be used with these two arrangements for attenuation figures of one and two decibels. Because of complications introduced in tuning, it is not recommended that these cable lengths be exceeded. With either of these arrangements, it should be noted that line-tuning equipment or its equivalent must be provided in the terminal apparatus or at the other end of the cable to handle the reactance components transmitted through the cable. However, since the impedance-matching transformer may be mounted in the base of the coupling capacitor, these arrangements do result in eliminating the outdoor-mounted line-tuning unit.

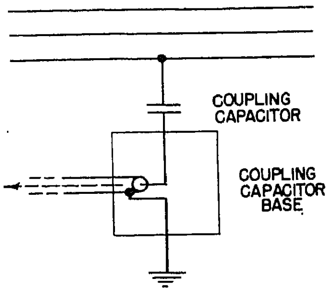


Figure 8. Coupling capacitor with concentric-cable lead-in brought directly into base

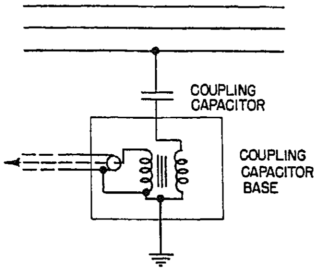


Figure 9. Coupling capacitor with impedance-matching transformer in base

By-Passes

In addition to their primary functions of connecting the carrier-terminal equipment to transmission lines, coupling capacitors and line-tuning equipment are also used extensively in maintaining carrier circuits around discontinuities such as transformers (including autotransformers) and open switches. This application is generally referred to as a by-pass.

Most of the problems encountered in applying by-passes are similar to those encountered at the terminals. If the coupling capacitors associated with the two sides of a simple by-pass are physically separated by a distance of over 100 feet, it is the usual practice to use a separate line-tuning unit with each capacitor to avoid excessive lengths in the leads from the coupling capacitors. Such an arrangement for a single frequency by-pass is shown in Figure 10. Here, it should be noted, the usual practice is to carry the connections between the two tuning units through a concentric cable. Where two frequencies are to be by-passed, the arrangement used in Figure 6 is used in place of each of the single-frequency units shown in Figure 10. Where more than two frequencies are to be by-passed, it is common practice to use additional coupling capacitors and line-tuning equipment. However, band-pass circuits such as shown in Figure 7 should be considered for three or more frequencies.

Frequently, in connection with party-line or system-broadcast types of carrier circuits, it is necessary to by-pass around possible open switches at the junction of several lines so that carrier circuits are maintained through more than two branches. Such an arrangement is the three-way by-pass shown in Figure 11. Here, it will be noted that the concentric cable leads from the three line-tuning units are simply paralleled. If a terminal station on the same channel is also located at the by-pass point, the connection into the circuit is made by paralleling the cable from the terminal equipment with the line-tuning units. In both of these cases there is an unavoidable impedance mismatch at the junction of these paralleled cables which introduces a small loss.

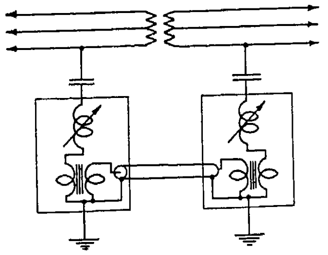


Figure 10. Two-way single-frequency ground-return by-pass

When the coupling capacitors associated with a two-way by-pass are physically separated by less than 100 feet, it is usually possible to simplify the by-pass arrangement considerably by including all of the line-tuning equipment in one cabinet, preferably located halfway between the two coupling capacitors. For a single frequency, the arrangement shown in Figure 12 is all that is required. For the higher carrier frequencies or for the higher values of coupling capacitance, a single variable inductor is often sufficient. The by-passing of two frequencies under the same condition involves the use of a two-frequency pass circuit similar to the one shown in Figure 6, omitting the impedance-matching transformer. For by-passing three or more frequencies through one tuning unit, a band-pass circuit such as Figure 13 may be considered.

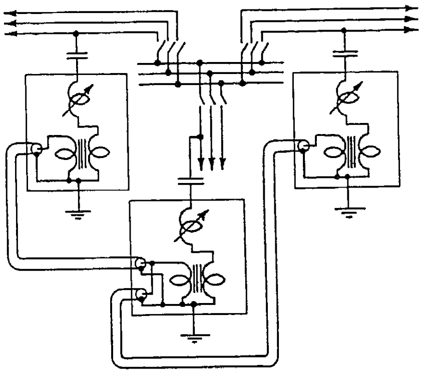


Figure 11. Three-way single-frequency ground-return by-pass

Line Traps

The third important tool used in applying carrier circuits to power lines is the line trap. In its simplest form, this device consists of an air-core reactor with a fixed or tapped capacitor connected in parallel to form a parallel resonant circuit. It is usually used in series with a conductor of the transmission circuit to provide a high impedance or "trap" for currents of the carrier frequency to which the resonant circuit is tuned. In this series position, the reactor of the trap must carry not only the normal line current but all abnormal or short-circuit currents that the line itself is called upon to carry. The conductor with which the reactor is wound must therefore be comparable in size or cross section with the line conductor, and the construction must be such as to withstand the mechanical stresses imposed by short-circuit currents. Its position in the line also exposes it to transient high-voltage disturbances attributable to lightning and switching, and adequate lightning arresters must be provided across both reactor and capacitor.

The impedance developed in the trap, at the resonant carrier frequency, is a function of the coil inductance and the carrier-frequency losses in the coil and its shunt capacitor. With capacitor types and coil construction normally used, peak or resonant-frequency impedances of well over 10,000 ohms are not unusual. However, these high peak values are only incidental to the primary objective of developing an impedance of at least 500 ohms or so over a band of frequencies. The width of this band required depends on the nature of the carrier channels to be trapped. Narrow-band channels, such as are used for pilot relaying or telemetering, require trap band widths of not wider than two or three kilocycles, while broad-band channels, carrying voice or audio-tone-modulated signals, require band widths of five or six kilocycles. Frequently, narrow-band channels can be located close enough together in the frequency spectrum to use a single broad-band trap to cover several channels.

The band width developed in the trap is largely a function of the total inductance available in the coil and for general use with both narrow-band and broad-band channels, a single-frequency trap requires a minimum coil inductance of approximately 150 microhenrys. If the total inductance available is in the order of 250 microhenrys, the coil may be double tuned to provide two high-impedance bands in the normal frequency range, each wide enough for a single broad-band channel. This two-frequency trap requires a more complex tuning arrangement, consisting of additional tapped capacitors and one or more auxiliary variable inductors. However, since the coil is the major item of cost in the line trap, this additional complexity is usually justified when two channels are to be trapped. Practical considerations of size, weight, and cost have limited largely the maximum inductance available in the line-trap reactor to approximately 250 microhenrys. This fact, together with the complexity and difficulty in adjustment involved, has made it impractical to tune a single trap coil to more than two frequency bands. Where it is necessary to trap more than two widely separated channels in a single line conductor, additional traps are generally used.

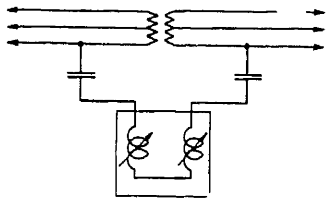


Figure 12. Simple single-frequency by-pass

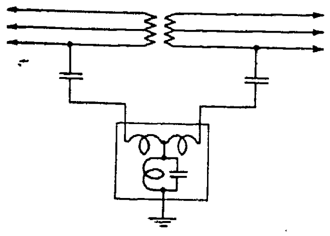


Figure 13. Band-pass by-pass circuit

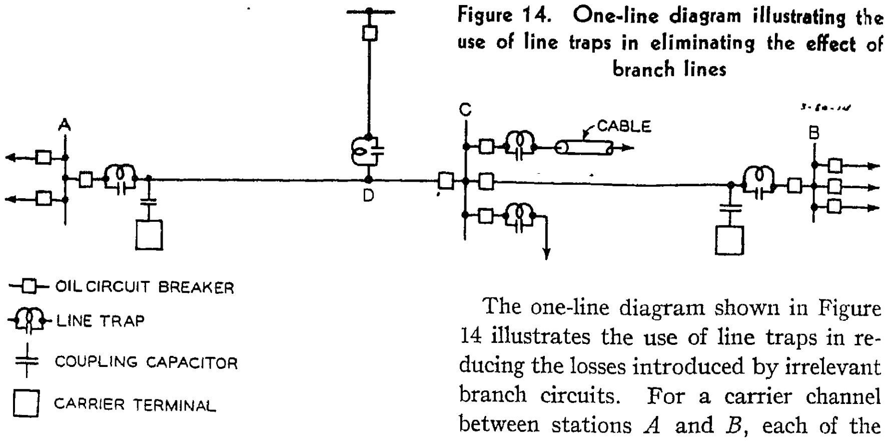


Figure 14. One-line diagram illustrating the use of line traps in eliminating the effect of branch lines

In general, the function of the line trap is to insure the reliability and improve the efficiency of carrier channels applied to a transmission line by confining the carrier-frequency currents to desired portions of the system or by minimizing the losses in low impedance loads or irrelevant branch lines and preventing interruption or interference from normal switching operations or faults. More specifically, the principal applications of line traps may be listed as follows:

1. To reduce transmission losses in irrelevant branch lines.
2. To minimize the effect of low impedance shunts.
3. To prevent interruption attributable to external faults or intentionally applied grounds.
4. To isolate the carrier channel effectively.

The one-line diagram shown in Figure 14 illustrates the use of line traps in reducing the losses introduced by irrelevant branch circuits. For a carrier channel between stations *A* and *B*, each of the branch circuits at *A*, *B*, *C*, and *D* may absorb a considerable amount of carrier-frequency energy as a shunt load, aside from possible undesirable reflection effects. The effects of the branches at *A* and *B* may be greatly reduced by means of two traps located as shown. At *C* and *D*, traps are used in each of the branch circuits.

Because of reflections, a short branch line, such as *D* of Figure 14, if over 1,000 feet or less than 20 miles in length, may have the effect of a very low impedance shunt at certain frequencies in the carrier range. The difficulty of determining accurately the frequencies at which the branch will appear as a low impedance and possible limitations in choice of a frequency to avoid this loss usually make it advisable to use a trap in each of these cases.

High-voltage cable circuits, such as shown for one of the branch circuits at station *C* of Figure 14, almost invariably require a trap. The extremely low characteristic impedance of such cable circuits, as compared with the line impedance, imposes a heavy load on the carrier circuit at all frequencies, and in addition it may cause undesirable standing-wave or reflection effects.

For certain classes of carrier channels, such as those used for pilot relaying, line traps are as essential to the circuit as the coupling capacitors or the terminal equipment itself. Their principal function in these circuits is to insure that an external fault does not short out the carrier circuit. This application is illustrated by Figure 15, showing a pilot-relaying carrier channel between stations *A* and *B*. If a fault occurs on the system at some point beyond *B*, such as at *X*, the line trap at *B*

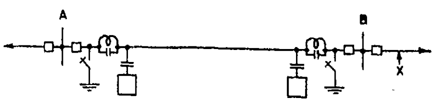


Figure 15. One-line diagram illustrating the use of line traps in maintaining the carri r circuit when faults or grounds are applied

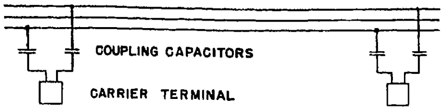


Figure 16. Single interphase coupling

will function to maintain a sufficient amount of impedance between the fault and the carrier circuit so that a normal carrier signal may be transmitted over the circuit during the fault.

Figure 15 also illustrates the use of line traps in maintaining a carrier circuit over a line that may be removed from service and grounded at its terminals. This is accomplished by locating the traps between the coupling capacitors and the grounding switches as shown. In some types of switchyard construction, where a grounding bus is used, the bus is insulated and tied to ground through one or more line traps.

In the case of certain types of carrier circuits it is desirable to isolate effectively the line conductor or conductors used at the carrier frequencies of the channel, by using traps at each terminal and in all branch circuits. Two-terminal circuits such as used for two-frequency duplex telephone channels, most frequently follow this practice. In addition to the usual gains in efficiency and reliability, this arrangement essentially clears the circuit of transmission irregularities and permits the circuit to be terminated properly by the terminal equipment. Under these conditions, definite channel frequencies may be assigned in advance of installation.

Methods of Line Coupling

There are five general types of line coupling commonly employed for single-circuit and double-circuit lines. These types are listed in the order of their general preference, based upon over-all reliability and ease of installation of the carrier channel, as follows:

1. Single interphase coupling.
2. Ground-return coupling.
3. Intercircuit coupling.
4. Double-circuit ground-return coupling.
5. Double-circuit interphase coupling.

Detailed descriptions of these various types of coupling follow.

SINGLE INTERPHASE COUPLING

Single interphase coupling, as shown in Figure 16, uses two of the three power phases of the transmission line as a path for the carrier energy; consequently, it is not affected by high ground resistances or variations in ground resistance as is any type of coupling which requires a ground-return path for the carrier energy.

In an interphase-coupled carrier circuit, conditions on the phase wires to which coupling is not made have negligible effect on the operation of the carrier channel. In addition, this type of coupling provides a circuit having the most stable operating characteristics, the lowest attenuation, and the lowest noise level. Its principal disadvantages lie in the cost and the construction complexity involved in providing coupling and line traps for two phases throughout the circuit, as well as the complexity of line-tuning equipment at terminals and by-passes. It has been used most extensively for long-haul circuits where its efficiency justifies its higher cost.

GROUND-RETURN COUPLING

Ground-return coupling utilizes only one phase wire of the transmission line and ground or any other suitable path for the return side of the circuit, as illustrated in Figure 17. If good grounds are present at the carrier terminals or if a ground wire is carried between the terminals, this type of coupling compares very favorably, in respect to line-attenuation losses, with interphase coupling.

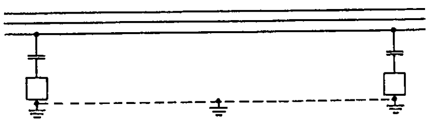


Figure 17. Ground-return coupling

The principal advantage of ground-return coupling is that it generally requires only half the number of coupling capacitors and line traps as required for full metallic coupling such as interphase. In addition, the line-tuning equipment required at terminals and by-passes is usually less complicated. Its main disadvantages lie in its somewhat higher attenuation and higher noise level as compared with the interphase circuit.

Another characteristic, sometimes considered a disadvantage for the ground-return circuit, is the fact that a considerable portion of the carrier energy uses the uncoupled phase wires of the line as a return path (in addition to the ground-return path); and, since definite provision is seldom made for maintaining this return path around transformers or open switches, the circuit may be subject to wider variations in attenuation.

For many types of carrier channels, the disadvantages of the ground-return circuit are greatly outweighed by its simplicity and lower cost. It is almost universally used for all pilot relay channels and extensively used on medium and short-haul circuits for all other types of carrier channels.

INTERCIRCUIT COUPLING

Where a double-circuit transmission line is available between carrier-terminal stations, it is sometimes desirable to use both lines for the carrier circuit to insure its continuity when either line is taken out of service. The intercircuit coupling arrangement shown in Figure 18 is used sometimes to provide this feature.

This is a form of interphase coupling in which coupling is made to one phase wire of one line and to a different phase wire of the other line of a double-circuit power line. It is only applicable on double-circuit lines which are *incapable of being sectionalized between the carrier terminals.* Where a suitable frequency is available to permit operation of the carrier channel with the increased number of switching configurations possible on the double-line channel, as compared with a single-line channel, this double-circuit type of coupling will provide uninterrupted carrier service with all phases of either line solidly grounded at any point. In this respect, it will provide most of the advantages of double-circuit interphase coupling with but half the coupling capacitors and line-tuning assemblies required by the double-interphase arrangement.

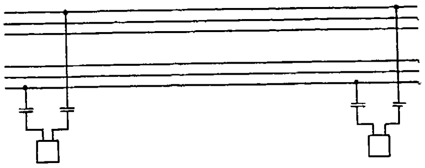


Figure 18. Intercircuit coupling

DOUBLE-CIRCUIT GROUND-RETURN COUPLING

The essential difference between double-circuit ground-return coupling, as shown in Figure 19, and intercircuit coupling is that the coupling is made to the *same* phase wire instead of a *different* phase wire in each line. Since earth or ground wire provides the return path for the carrier energy, this type of coupling can be applied even where the double-circuit lines are sectionalized between carrier terminals. That is, it will provide uninterrupted operation with either line open, or, in most cases, with either line grounded.

DOUBLE-CIRCUIT INTERPHASE COUPLING

This type of coupling, shown in Figure 20, provides the possible advantages of both double-circuit ground-return coupling and intercircuit coupling but requires twice the number of coupling capacitors, line traps, and line-tuning units as these former types of double-circuit coupling. The small advantage gained with double interphase coupling makes it difficult to justify its comparatively high cost.

The preceding comments assume that the coupling capacitors are connected to the conductors on the line side of the station breakers and disconnecting switches. In a limited number of cases, particularly where it is desired to couple to several transmission lines at one location, the coupling capacitors have been connected to the station high-voltage bus. The "bus coupling" may be ground-return, as shown in Figure 21, or interphase.

Any advantage of bus coupling is in the reduction in the quantity of coupling capacitors required. However, the proper use of line traps is often impossible with bus coupling, and the opening of any breaker interrupts the carrier channel over that particular transmission line. Also, the common use of multiple busses, or busses that can be sectionalized, frequently makes the arrangement impractical. These restrictions seriously limit the use of bus coupling and reduce or eliminate its apparent economic advantage.

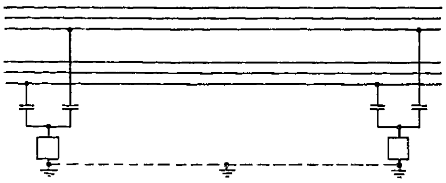


Figure 19. Double-circuit ground-return coupling

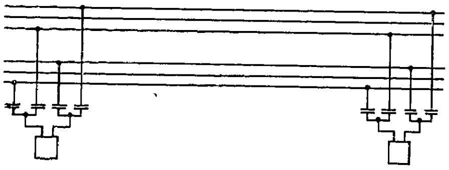


Figure 20. Double-circuit interphase coupling

Selection of Frequency

The frequency range of from 50 to 150 kilocycles has been recognized as standard for power-line-carrier equipment for a number of years; for this reason, the bulk of application effort will probably be concerned with carrier channels in this range. However, there are conditions occasionally encountered that require consideration of frequencies outside of this standard range, even at the expense of special equipment. Some systems have already become so crowded with channels in this range that new channels can be added only by extending the range somewhat, both up and down, or modifying older apparatus to modern standards of selectivity. However, by careful advance planning in the selection of channel frequencies, it is usually possible to provide for all of the channels required on a system in the range available with standard equipment.

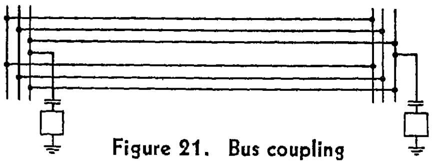


Figure 21. Bus coupling

There are several factors that should be considered in selecting a frequency for a new channel. If there are already carrier channels in use on the system or on adjoining interconnected systems, the question of possible interference with these other channels is usually of primary importance. The spacing of channels is largely dependent on the type of channels involved, the selectivity characteristics of the carrier receivers and the carrier power levels. Wide-band channels such as required for telephone or tone-modulated control and telemeter systems may require a minimum band width of five or six kilocycles with a minimum separation of channels running from 10 to 20 kilocycles. Narrow-band channels such as required for pilot relaying or for frequency-shift-type telemetering may require only one to two kilocycles of the frequency spectrum and, under favorable conditions, may be spaced only one or two kilocycles apart.

For some types of channels, such as are used for single-frequency party-line telephone systems, reflections attributable to untrapped branch lines or short tap lines may limit the choice of frequencies that will give satisfactory transmission between all stations under a variety of line-configuration conditions. Often, in these cases, it is necessary to make circuit-frequency-characteristic measurements before the best frequency can be selected. For most medium and short-haul circuits, however, reasonable judgment in the use of line traps will usually permit a wide choice of frequencies.

Where several types of carrier channels are to be operated on the same system, it is usually a good plan to reserve the lower end of the band for the long-haul circuits to realize the advantage of lower line attenuation. The short-haul circuits can often stand the additional attenuation at the higher frequencies.

When it is found necessary to use frequencies below 50 kilocycles, they should be used only for narrow-band channels, where possible, because of the increased difficulty in getting satisfactory wide-band characteristics in tuning units and traps.

Carrier-Circuit Attenuation

The attenuation or dissipation of the carrier-frequency energy in the course of its transmission from a carrier transmitter at one terminal to a carrier receiver at another terminal may be considered as analogous to voltage drop in 60-cycle power transmission. However, where voltage drop at 60-cycles is usually expressed in per cent, the ratios of transmitted volts and watts to received volts and watts at carrier frequencies is so great that it is most conveniently expressed in decibels, which is a measure of the ratios on a logarithmic scale. A power ratio of 10,000, corresponding to an attenuation of 40 decibels, is not uncommon in transmitting carrier energy over long lines. With this amount of attenuation, a transmitted power of ten watts would appear at the receiver as only 0.001 watt.

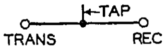
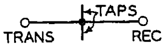
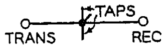
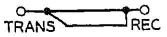
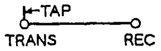
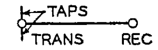
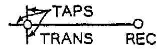
Fortunately, the carrier receiver is essentially a voltage-actuated device, requiring a negligible amount of carrier energy at a fraction of a volt on its input terminals to produce the desired output. Compared to a standard broadcast radio receiver, however, the carrier receiver is relatively insensitive. Attributable to the relatively high noise level present on the average transmission system, the maximum sensitivity that can be used in the receiver is limited. The received carrier signal must therefore arrive at the receiver input terminals at a level sufficiently high to establish a satisfactory signal-to-noise ratio.

Another fortunate feature of the modern carrier receiver is its automatic volume control. Practically all types of carrier receivers now have some form of automatic volume control that functions to produce an essentially constant and usable output for a wide variation in input signal. Some types of carrier telephone receivers are in use that will hold the audio-output-level variation within five decibels for a carrier-input-level variation of up to 40 decibels.

From the foregoing considerations it would appear that the application of a carrier circuit would involve such deep-seated problems as determining how much transmitter power is required to produce a certain voltage on the receiver input terminals. In order to simplify the problem greatly, it has become standard practice to rate a transmitter-receiver combination, normally used to operate together in forming a carrier channel, in terms of the maximum attenuation in decibels through which the combination will operate successfully. This rating takes into account three factors: the receiver sensitivity, the automatic-volume-control range, and the transmitter output. The problem therefore resolves itself into one of laying out a circuit that will have a total estimated attenuation within the decibel rating of the equipment it is desired to use for the channel.

The process of estimating the total attenuation for given carrier circuit is simply a matter of determining a value for each of the components involved and adding them up. The principal attenuation components making up the total and the methods used in evaluating each are given in the following list and discussion.

Table III. Attenuation Introduced by Miscellaneous Sources

Item	Source of Attenuation		Decibel Loss
A	Line-tuning and coupling equipment		1
B	Untrapped single tap or spur line		3.5
C	Untrapped double tap or spur line		6
D	Untrapped triple tap or spur line		8
E	Untrapped loop		7
F	Additional bridged terminals on same carrier channel, each		1
G	Terminal located at junction with one untrapped tap line		3
H	Terminal located at junction with two untrapped tap lines		5
I	Terminal located at junction with three untrapped tap lines		6

1. Attenuation in Lead-in Cable at Transmitter Terminal

This component is the loss in the concentric cable between the transmitter terminals and the terminals of the line-tuning or coupling equipment. When the line tuning is located with the coupling capacitor and the cable is properly matched through an impedance-matching transformer at the tuning unit, this loss is determined from Figure 22 by multiplying the cable length by the attenuation per 1,000 feet indicated for the carrier frequency selected. If the frequency has not been determined, the value for 85 kilocycles may be used as an average figure.

If the line-tuning equipment is mounted with the terminal equipment and the connection to the coupling capacitor carried directly to the coupling through a concentric cable, the loss should be estimated from the values indicated in Table II.

When both the terminal and line-tuning equipment are mounted together and located near the coupling capacitor, no loss is included for this component.

2. Line-Tuning and Coupling Losses at Transmitter Terminal

The losses in the line-tuning and coupling equipment will vary somewhat with the complexity of the tuning circuit and the frequency used. However, since this loss is small and usually less than one decibel, the usual practice is to neglect the variations and use a value of one decibel as shown for item *A* of Table III.

3. Transmission-Line Attenuation

The attenuation of the carrier energy, in transmission over the power line itself, is influenced by a number of factors; principal among these are the size, spacing, insulation, and disposition of the line conductors, the carrier frequency used, the type of coupling used, that is, whether it is ground-return, interphase, and so forth. For practical purposes, some of these variables can be neglected and the others combined to form the simple set of curves and charts given on Figure 23.

The attenuation values indicated are based on the use of copper or the equivalent steel reinforced aluminum cable conductor and full-metallic coupling. For ground-return coupling, the values should be increased by 20 per cent. The values indicated are purposely made somewhat higher than actual measured values taken on de-energized lines, to include additional miscellaneous losses contributed by connected power equipment.

In using Figure 23, if the line voltage and conductor sizes to be used do not coincide with one of the line descriptions listed, the correct value to use for the attenuation per mile may be determined by interpolating between curves or by using the next higher curve. The geographical line length, neglecting sag and topography, may be used with sufficient accuracy. If the frequency of the channel has not been selected, the value for 85 kilocycles may be used as an average figure.

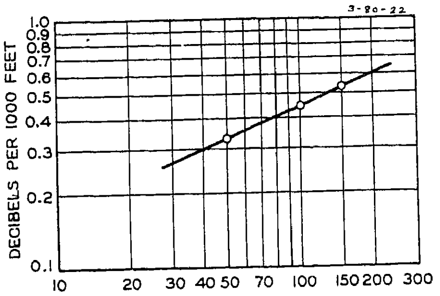


Figure 22. Attenuation characteristics of concentric lead-in cable

4. Attenuation Attributable to Irrelevant Branch Lines

If the transmitter or receiver terminal is located at a line-junction point and the carrier energy is permitted to dissipate over one or more paths, not included in the carrier circuit under consideration, the attenuation introduced may be found under *G*, *H*, and *I* of Table III.

Untrapped spur or branch lines on the circuit between transmitter and receiver terminals will introduce similar losses which may be obtained from *B*, *C*, and *D* of Table III.

Untrapped loop circuits between the transmitter and receiver terminals may introduce the large loss indicated for *E* in Table III, at certain frequencies. Unless it is essential that the alternative paths provided by the loop be maintained, the usual practice is to insert a line trap

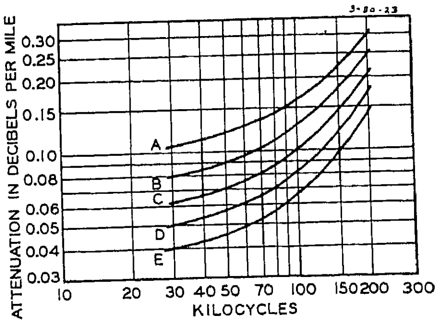


Figure 23. Attenuation introduced by the transmission line

Line Voltage (Kilovolts)	Conductor Size	Curve
34.5	Number 4 average	*A*
34.5	Number 2/0 average	*B*
69	Number 4 average	*B*
69	Number 4/0 average	*C*
115–161	Number 0 average	*C*
115–161	1-inch diameter	*D*
220–287	1-inch diameter	*E*

Table IV. Multiplying Factors

Type of Carrier Channel	Multiplying Factor
Telephone	1.3
Pilot relaying	1.5
Telemetering, high accuracy	1.5
Telemetering, indication only	1.3
Supervisory control	1.5
Load control	1.3

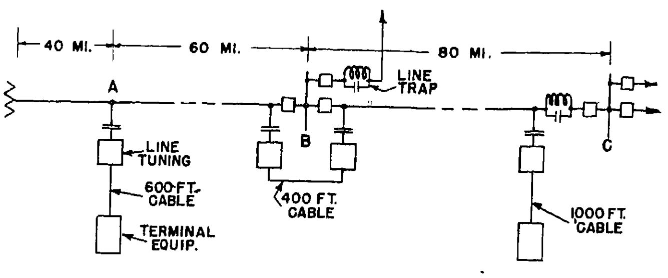


Figure 24. One-line diagram showing typical circuit application

at one end of the loop and treat the remainder as a single branch line.

The values indicated in Table III for circuit configurations are based largely on losses caused by energy being absorbed by these loads on the circuit and do not include additional losses which may be introduced at certain frequencies attributable to reflections caused by short (less than 20 miles) untrapped spur lines. It is usually not considered good practice to leave more than two such short spur lines untrapped even when a wide choice in frequency is available. If the choice in frequency is already limited by the presence of other carrier channels, it is good practice to trap all such short spur lines.

5. Attenuation Attributable to Connected Power Apparatus

Most high-voltage power transformers appear to the carrier circuit as a relatively high capacitive reactance, and a line terminating in a transformer behaves at carrier frequencies essentially as though it were terminated in an open circuit. Such a small amount of carrier energy is transmitted through a transformer that it is seldom necessary to trap it. It is most convenient to include the small amount of loss in transformers and other power apparatus as an average figure making up a small part of the line loss. It is therefore usual practice not to include a specific item for losses in connected power apparatus.

6. Attenuation Attributable to By-Pass Circuits

The attenuation introduced by losses in each by-pass around transformers or open switches may be obtained by considering the loss in each line-tuning and coupling equipment and the loss in the interconnecting concentric cable, when used. The loss in each tuning unit is given under *A* of Table III, and the loss in the cable is obtained from Figure 22.

7. Line-Tuning and Coupling Losses at Receiver Terminal

Line-tuning and coupling-equipment losses at the receiver terminal are treated in the same way as similar losses at the transmitter terminal and calculated as indicated for item 2 of this list.

8. Attenuation in Lead-In Cable at Receiver Terminal

This loss is treated in the same way as for the transmitter terminal, under item 1 of this list.

9. Multiplying Factors

It is a well-known fact that the accumulation of ice, sleet, snow, and frost on the conductors and insulators of open-wire telephone lines may increase the attenuation of such circuits enormously at carrier frequencies.[2] The increase in attenuation of a power-line-carrier circuit as a result of formations of ice, sleet, snow, or frost on line conductors and insulators has not as yet been determined accurately; however, general experience has indicated that this increase is much smaller than experienced on telephone lines and, in general, can be accommodated easily in the receiver automatic volume control.

The accumulation of dust and oily or sooty deposits on line insulators also has the general effect of both increasing the attenuation and increasing the noise level on a power-line carrier circuit.

The most satisfactory method of accommodating these variations and insuring that they fall within the automatic-volume-control range of the carrier receiver is through the use of a multiplying factor. Since the effect of attenuation variation and the effect of abnormal line noise will vary with the type of service rendered by a carrier channel, different multiplying factors are applied for different types of carrier channels as listed in Table IV.

As the last operation, therefore, in determining the total circuit attenuation, the total of the several attenuation components as covered by items 1 through 8 should be multiplied by the proper factor selected from Table IV.

Example

In order to illustrate, more clearly, the complete procedure in arriving at the total attenuation figure for a carrier circuit, a typical circuit such as Figure 24 may be taken as an example. Assuming that a telephone channel is to be established between stations *A* and *C* over a line distance of 140 miles on a 69-kv 4/0 line, using a carrier frequency of 100 kilocycles and interphase coupling, the components and totals would work out as follows:

Lead-in cable at *A* (Figure 22)	0.27 decibel
Line tuning and coupling at *A* (Table III, item *A*)	1.00 decibels
Untrapped spur line at *A* (Table III, item *G*)	3.00 decibels
Transmission line from *A* to *B* (Figure 23)	6.00 decibels
By-pass at *B* (Figure 22 and Table III)	2.18 decibels
Transmission line from *B* to *C* (Figure 23)	8.00 decibels
Line tuning and coupling at *C* (Table III)	1.00 decibels
Lead-in cable at *C* (Figure 22)	0.45 decibel
Total attenuation components	21.90 decibels
Multiplying factor (Table IV)	1.3
Total attenuation	28.47 decibels

Based on the preceding estimate, a transmitter-receiver combination having a rating of 28.47 decibels or more could be used for the channel.

References

1. Carrier-Current Relaying and Communication on the TVA System, **M. Stevens Merritt, T. DeWitt Talmage.** Electrical Engineering, volume 61, November 1942, pages 561–72.

2. Open-Wire Line Losses, **L. T. Wilson.** Bell Laboratories *Record*, November 1937.

3. Multichannel Carrier-Current Facilities for a Power Line, **P. N. Sandstrom, G. E. Foster.** AIEE Transactions, volume 61, 1942, February section, pages 71–6.

4. Carrier Attenuation Discloses Glaze Formation, **G. G. Langdon, V. M. Marquis.** *Electrical World*, August 12, 1939, pages 38–40, 100–01.

5. Symposium on Operation of Boulder Dam Transmission Line—Carrier-Current Equipment, **J. D. Laughlin.** AIEE Transactions, volume 58, 1939, April section, pages 147–51; discussion, page 156.

6. Application and Performance of Carrier-Current Relaying, **Philip Sporn, Charles A. Muller.** AIEE Transactions, volume 57, 1938, March section, pages 118–24.

7. A New Carrier-Current Coupling Capacitor, **E. D. Eby.** AIEE Transactions, volume 54, 1935, August section, pages 848–52.

8. Carrier-Current Losses, Measured and Interference Minimized on Boulder–Los Angeles Transmission Lines. **J. D. Laughlin, W. E. Pakala, M. E. Reagan.** AIEE Transactions, volume 59, 1940, January section, pages 4–9; discussion, page 9.

Safe Ratings for Overhead Line Conductors

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Synopsis: Many factors must be considered in establishing safe ratings for overhead copper line conductors. Most important is the low-temperature annealing and loss of strength of the hard-drawn copper itself. Others are the effect of atmospheric conditions and line location relative to structures and topographical features tending to obstruct the wind. The actual construction details of the line, such as ground clearances, conductor splices, and connectors are pertinent, as are the policies of the personnel responsible for maintaining the line.

This paper attempts to show how these factors can be evaluated to establish operating ratings, indicating how readily available data can be utilized, pointing out the lack of completely satisfactory data on other points. Procedure is stressed rather than results, as the influence of local conditions upon many of the factors precludes publication of ratings which are entirely safe to apply without careful investigation of the effect upon the particular copper conductors to which they are to be applied.

WARTIME urgency is focusing increasing attention upon utmost power to defense plants with minimum critical material to reinforce the bulk power system. Quite logically pressure is being applied to raising the operating limits of many types of equipment. The need is unquestionable, but extreme caution is essential lest the very effort to assist our war production by allowing increased ratings prove to jeopardize the service we are endeavoring to render. This need for caution applies, of course, to all component parts of the electric-power system, but particularly to the bare conductors of the overhead transmission system where it is too easy to overlook the seriousness of permanent impairment by overheating. This paper is intended to discuss that phase of the problem with the aim of presenting a complete analysis of one commonly used conductor in such form as to facilitate similar analyses of any other copper conductor. The author has endeavored to collect all the pertinent data that are available and to present the combined results in a form suitable for determining safe ratings. Several phases of the analysis are inadequately supported by factual data and will be indicated as assumptions in the hope that laboratory test programs may be arranged to determine the exact relationships.

The allowable rating for any overhead conductor depends upon at least eight factors, as follows:

1. The ability of the conductor to withstand the resulting temperature without excessive loss of mechanical strength by annealing.
2. The ability of clamps, connectors, and joints on the conductor to withstand the resulting temperature without oxidation of the contact surfaces and local heating in excess of the allowable conductor temperature.
3. The adequacy of span clearances to permit the additional sag caused by the conductor temperature associated with the proposed rating.
4. The problem of providing short-circuit and overload protection to operate within the margin between the current rating and destructive overloads.
5. The adequacy of substation and terminal equipment to carry the currents for which the transmission conductors may be rated.
6. Operating problems of the bulk power system with individual circuits rated to high values.
7. The ability of voltage-regulating equipment to compensate for the voltage variations which accompany heavily loaded lines.
8. The considerable energy losses resulting from heavy line currents.

No engineer would contend that all eight are of equal importance, but neither can he afford to ignore any one until he has satisfied himself that it does not limit the particular line which he wishes to rate.

Methods for Raising Limitations

Methods are available to improve many of these limiting conditions to the point where other considerations establish limitations at higher current ratings. In general, increased vigilance is essential, as many of the limitations depend upon visual observation for detection, and operation at higher current ratings should be expected to require more frequent inspection and maintenance.

One company, desiring to carry heavier loads on a 66,000-volt 336,400-circular-mil steel reinforced aluminum cable transmission circuit, ordered close inspection of the dead-end clamp, loop, and insulator assemblies on several structures. This inspection revealed conductor strands damaged in the dead-end clamps to the point where heavier loading probably would have caused failure, and parallel-groove connectors distorted to the point where contact pressure had been relaxed and strands had welded to the contact surfaces. The circuit was rebuilt with compression-type dead-end clamps and improved connectors, then raised to the higher rating.

After that discovery, the policy has been established of thoroughly inspecting and overhauling any circuit before it is released for operation above a nominal rating. Tests in still air at currents up to 750 amperes on 4/0 conductors and various types of connectors indicated that the newer and heavier bronze parallel-groove connectors and the *U*-bolt type bronze connectors operate much cooler than the 4/0 conductors themselves and appear to be adequate for any currents that can be applied safely to the conductors. Automatic line splices in tension seem to be satisfactory, but the old served joints have been found inadequate. Split-bolt connectors of the types used for distribution construction cannot be recommended for heavily loaded transmission lines. Further tests may prove the adequacy of more devices, but methods are available now to eliminate clamps and connectors as restrictions to the rating of copper conductors for conductor temperatures up to at least 100 degrees centigrade.

Magnetic heating of suspension and strain clamps may be a serious problem. W. H. Burleson[1] has reported tests on 500,000-circular-mil stranded copper conductor indicating conductor temperatures inside the usual malleable iron suspension clamps as much as 100 degrees Fahrenheit higher than the conductor temperature outside the clamps, evidently caused by magnetic losses in the iron. His tests indicated also that the magnetic heating is reduced considerably by substituting nonferrous *U*-bolts in the malleable iron clamps and virtually eliminated by the further substitution of a nonferrous keeper. Similar tests by other experimenters using large aluminum conductors without armor rods in malleable iron suspension clamps show substantially the same magnetic heating, with consider-

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able reduction when armor rods were added to the conductor in a correspondingly larger clamp. Although the effect seems less pronounced with smaller conductors and correspondingly lower currents, there is ample reason to fear that magnetic heating in conventional suspension and strain clamps may raise the temperature of copper conductors well beyond the limit established by annealing. Suspension clamps also have been found within which the copper conductor has been burned badly, apparently caused by loosening of the clamps to the point where arcing has occurred between the conductor and the clamp. Every suspension and strain clamp thus becomes subject to investigation before high current ratings can be allowed, and two methods offer possibility of correction, namely:

1. Replace or rebuild the clamps to eliminate the closed magnetic paths and provide reliable contact between the clamps and the conductor.

2. Install additional conductor material, possibly in the form of armor wires, through larger all-ferrous clamps to reduce the resistance loss and to aid in dissipating the magnetic heating.

Many transmission lines have been designed for safe clearances to ground and structures under the spans at conductor temperatures of 50 degrees centigrade or less. Raising the maximum operating temperature to 100 degrees centigrade causes a considerable increase in length by expansion of the conductors and reduces clearances to ground by nearly four feet in a 1,000-foot span. Obviously, then it is important to ascertain that the line has ample clearances before the operating ratings are increased. Occasionally additional clearance can be secured by increasing the tension, but this method must be applied with due consideration to the strength of supporting structures and recognition of the fact that higher design tensions allow less margin for annealing and thus reduce the maximum current rating that can be allowed. There remains always the possibility of raising the point of attachment on structures by remodeling the structure or sometimes by changing the conductor attachment from suspension to strain. Clearance at elevated temperatures may be a problem, but several methods are available to eliminate it as a final limitation to higher current ratings.

Currents of the magnitude caused by line faults may be exceedingly destructive if allowed to persist, as they tend to cause very high conductor temperature. Most protection in common use depends upon some form of overcurrent relays to discriminate between load currents and fault currents, the principal exception being several adaptations of differential protection such as pilot-wire or carrier and double-circuit balanced protection. Obviously some portion of the useful life of the conductor must be allocated to short-circuit operation, the amount depending upon how fast and how close above load-current ratings the fault protection can be maintained and upon the frequency and magnitude of the faults. Conservation of conductor life of heavily loaded circuits thus becomes an additional argument for improved lightning protection to reduce the number of faults, and improved relay and breaker protection to reduce the conductor heating caused by the fault currents.

No increase in transmission conductor rating is useful unless all associated equipment is adequate. The entire circuit must be reviewed from substation bus through the disconnect switches, circuit breakers, metering and relay equipment, and leads to the transmission line conductors, thence through any intermediate switching stations to the substation at the other end of the circuit. Numerous investigations have disclosed disconnect switches which failed to pass test at the higher currents without reinforcing or silver plating of contacts or enlarging the leads near the switch terminals to prevent heat flow from the conductors into the switch contacts. Some circuit breakers have required additional contacts or larger bushings, and metering transformer ratios, and meter scales have been changed. Many such limitations may be found, most of which could cause serious trouble if overlooked but can be corrected quite easily if detected before the rating is raised.

Table I. Determination of Equivalent Ambient Temperatures for Copper Conductors

Month	Greatest Monthly Mean Temperature (Degrees Fahrenheit)	Greatest Monthly Range of Daily Mean Temperature (1933–42) (Degrees Fahrenheit)	Greatest Daily Range of Hourly Temperatures (Degrees Fahrenheit)	Daily Correction (Degrees Fahrenheit)	Monthly Correction (Degrees Fahrenheit)	Equivalent Ambient Temperature: Monthly (Degrees Fahrenheit)	Equivalent Ambient Temperature: Seasonal (Degrees Fahrenheit)	Equivalent Ambient Temperature: Annual (Degrees Fahrenheit)
December	45.6	48.0	49.0	+7.6	+6.4	59.6	59.0	76.0
January	44.4	51.0	51.0	+8.0	+7.0	59.4		
February	42.7	51.0	52.0	+8.2	+7.0	57.9		
March	50.7	47.0	44.0	+6.2	+6.2	63.1	66.6	
April	57.2	41.0	48 0	+7.3	+4.9	69.4		
May	69.5	34.0	48.0	+7.3	+3.4	80.2	84.4	
June	75.0	32.0	37.0	+4.4	+3.1	82.5		
July	80.3	26.0	37.0	+4.4	+2.0	86.7		
August	78.7	25.0	37.0	+4.4	+1.9	85.0		
September	77.2	37.0	39.0	+4.9	+4.0	86.1		
October	62.9	42.0	41.0	+5.4	+5.1	73.4	70.6	
November	51.6	52.0	52.0	+8.2	+7.3	67.1		

Operating problems of a power system become increasingly involved as the relative importance of operating units is increased. When a transmission circuit forms part of a loop or network, it is customary to maintain service to all the load through the remaining circuits during any single circuit outage. As the load carried by any one circuit is increased, the provision of reserve capacity in the other facilities which must carry the load during outages of the heavily loaded circuit becomes increasingly difficult. In some cases the point is reached where the only recourse is to lighten the load by dumping, which would be intolerable where essential service would be interrupted. In this manner, operating problems may become a critical aspect requiring extensive analysis before an increased rating can be permitted to supplant system reinforcement.

Transmission circuits designed for operation at lower ratings frequently have no provision for voltage-regulating equipment to compensate for transmission regulation or have been provided with synchronous capacitors or other equipment of limited range. Heavier loadings may be expected to cause greater voltage variations, possibly in excess of the maximum for which the system was designed to operate. This aspect is not likely to be critical in metropolitan systems but may easily constitute a problem on more extensive lines. System stability may be involved, also, and these two factors should be investigated together. Many methods are available, however, to provide relief in case either one should prove a limitation.

Energy losses at the ratings indicated by this paper amount to 100 kilowatts or more per mile of circuit. The cost of the system investment and energy thus lost from useful service in some cases may justify the provision of additional facilities in lieu of remodeling existing circuits for increased ratings. In other cases, installation of power-factor-corrective equipment may be indicated.

Limitations of the conductor itself have been left for final consideration. Overhead conductors depend upon their inherent mechanical strength for support. Many conductors now in service have been weakened by flashover burns and broken strands, which tend to increase the heating at points already reduced in strength but not sufficiently damaged to justify repair. Heavier current loading means increased temperature in the conductor with a tendency to approach some limit as yet not defined. Whereas all of the other limiting factors have been found susceptible to relief, however, it seems probable that the conductor itself will prove to be a final limitation which cannot be relieved by any method short of replacement.

Table II. Average Wind Velocities

Month	Monthly Average (1940–42)	Seasonal Average	Annual Average
December	11.6	11.8	10.5
January	11.9		
February	12.0		
March	12.7	11.8	
April	11.0		
May	10.0	9.0	
June	9.2		
July	8.8		
August	8.4		
September	8.8		
October	10.0	10.9	
November	11.8		

Relationship Between Current and Conductor Temperature

It is reasonable to expect that the ultimate limit to the current-carrying capacity of a conductor will be established by temperature rise caused by energy losses to the point beyond which permanent damage begins. The first approach to the solution, therefore, involves a determination of the heat energy which can be dissipated into the atmosphere at various conductor temperatures. It is necessary to consider

1. Atmospheric temperature.
2. Wind velocity and direction relative to conductor.
3. Atmospheric pressure.
4. Effect of solar radiation.
5. Conductor surface thermal emissivity constant.
6. Conductor outside diameter.

Atmospheric temperatures ranging from −20 degrees Fahrenheit to 103 degrees Fahrenheit have been recorded by the United States Weather Bureau at Pittsburgh. For our purpose, however, an annual equivalent ambient temperature will be derived, using the method proposed by W. C. Sealey[2] with the aging curve of hard-drawn copper. Pertinent Weather Bureau data and calculated values are shown as Table I.

Plotting the various temperatures of Table I as Figure 1 serves to demonstrate the conservatism of this method for determining the equivalent ambient temperatures. Pittsburgh temperature data are recorded well above the ground under conditions closely resembling those prevailing along the transmission system, hence should be a satisfactory basis for establishing current ratings. It is proposed subsequently to use the annual equivalent ambient temperature in determining the relationship between current and temperature.

Wind velocity likewise is important as an aid to dissipating heat from the conductors. No way has yet been found to establish the effectiveness of wind independent of other variables, however, and it is necessary to resort to approximations. Weather Bureau records during the three-year period, 1940–2 inclusive, show wind velocities from practically zero up to 46 miles per hour, with an average velocity of 10.5 and a slight but consistent seasonal variation. These data are shown as Table II.

Subsequent formulas will show that wind velocity is effective approximately as the square root in dissipating heat, hence the squared average of the square roots of the hourly observations would be more suitable than the arithmetical average shown in Table II. Such data are not readily available, but computation from the hourly readings of wind velocity for several typical months indicates the squared average root of the wind velocity to be approximately 90 per cent of the arithmetic average.

Wind direction also is important insofar as it affects the angle of incidence to the conductors. With most lines having sections at considerable angular displacement from the general source-to-destination direction, it is not feasible to attempt to consider the specific direction of the wind. Marked deviation from the perpendicular has comparatively small influence, however, as wind at any angle from 30 degrees to 150 degrees should be at least equivalent to a perpendicular wind of 50 per cent of the velocity. No information has been found which could be applied to a wind parallel with the conductors, but when the angular shifting of even a steady wind is considered, together with the catenary curvature of span wires and the effect of ground contours in setting up local turbulence, it seems reasonable to assume that wind in any direction can be represented by a perpendicular wind with 50 per cent of the velocity. Following that assumption, wind velocities will be used at 50 per cent of the squared average root of the recorded values, and the direction disregarded.

Still another factor having considerable influence upon the cooling effect of winds is the shielding effect of high ground and buildings near the line. A good example is found in the ten per cent differential between the monthly average wind velocity recorded at the Allegheny County Airport situated on high ground and simultaneous recordings on the roof of a large building in the valley at Pittsburgh. For 66-kv

Table III. Effect of Five Degrees Centigrade Solar Heating During Sunshine Hours Upon Equivalent Ambient Temperatures

Month	Equivalent Monthly Ambient Temperature (Degrees Fahrenheit)	Sunshine Time (Per Cent)	Solar Heating Correction (Degrees Fahrenheit)	Equivalent Effective Temperature: Monthly (Degrees Fahrenheit)	Seasonal (Degrees Fahrenheit)	Annual (Degrees Fahrenheit)
December	59.6	11.6	+1.0	60.6	60.5	79.1
January	59.4	12.8	+1.2	60.6		
February	57.9	17.4	+2.3	60.2		
March	63.1	22.4	+2.7	65.5	69.6	
April	69.4	27.8	+3.4	72.8		
May	80.2	35.7	+4.1	84.3	88.7	
June	82.5	40.2	+4.3	86.8		
July	86.7	40.6	+4.2	90.9		
August	85.0	36.2	+4.1	89.1		
September	86.1	32.1	+4.0	90.1		
October	73.4	25.3	+3.0	76.4	73.0	
November	67.1	16.7	+1.7	68.8		

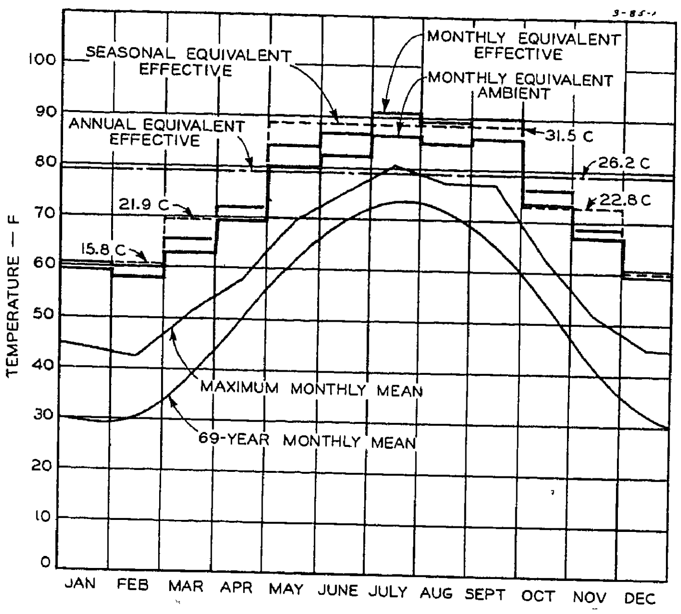


Figure 1. Equivalent ambient temperatures for copper conductors and equivalent effective temperatures including solar heating, Pittsburgh district

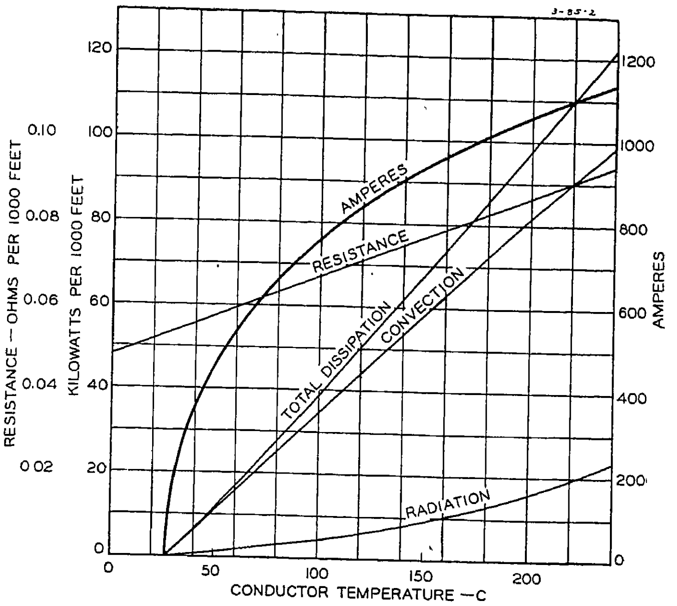


Figure 2. Curves for 4/0 bare stranded copper conductor

1. Watts dissipated
2. Resistance
3. Current at various conductor temperatures for equivalent effective atmospheric temperature (including solar heating) = 26.2 degrees centigrade, and effective crosswise wind velocity, 40.5 per cent of airport average = 4.25 miles per hour

transmission circuits, located as they commonly are on hill tops and in open country, the Weather Bureau data from the downtown Pittsburgh observation post should be amply conservative, and no other allowance for shielding seems necessary. When airport data are used, they will be reduced ten per cent to approximate corresponding data as they would have been recorded at the downtown location. Lower-voltage lines, particularly when located in built-up urban areas, definitely require an allowance for the shielding effect of buildings and hillsides, however, and tests by Schurig and Frick[3] seem to indicate that only approximately 25 per cent of the undisturbed wind velocity would reach the line.

Summarizing these wind-velocity factors, average wind velocities recorded at the airport are reduced to 40.5 per cent of the recorded values when applied to exposed 66-kv lines, to 10.1 per cent when supplied to shielded lower-voltage lines. Hourly wind velocities recorded at the downtown Pittsburgh observation post are reduced to 50 per cent of the recorded values when applied to exposed 66-kv lines, to 12.5 per cent when applied to shielded lower-voltage lines.

Atmospheric pressure enters the formula for heat dissipation as the square root. The normal variations in any one location do not often exceed plus or minus two per cent, hence have little effect upon the heat conductivity. Altitude reduces the atmospheric pressure nearly four per cent for each 1,000 feet above sea level, however, and should be included in the calculations for many localities. In the vicinity of Pittsburgh, for example, the altitude of transmission conductors is close to 1,000 feet above sea level and the normal pressure is 0.96 atmosphere.

Solar radiation tends to raise the temperature of conductors exposed to sunlight above that normally associated with the losses generated by current flow and the atmospheric temperature and wind velocity. Of course the effective surface of a cylindrical conductor is only $1/\pi$ times the total exterior surface and any rise in temperature results immediately in greater dissipation of heat to the surrounding air and other masses so that the added temperature rise from solar radiation should be small. Schurig and Frick[3] observed a two to eight degrees centigrade temperature increase attributable to solar radiation with the higher values applying only to conductors starting at atmospheric temperature. Several other sources show temperature increases of ten degrees centigrade for dark surfaces otherwise at essentially atmospheric temperatures. As an extreme condition, therefore, it will be assumed that loaded overhead conductors are subject to a five degree centigrade rise in temperature during all the hours in which sunshine is reported by the Weather Bureau.

Solar heating is very similar in effect to a corresponding increase in the atmospheric temperature. Arbitrarily adding nine degrees Fahrenheit (five degrees centigrade) to each monthly equivalent ambient temperature for the normal hours of sunshine for that month, then combining the two blocks by the method used by Sealey[2] for combining different months gives an equivalent which will be called the "monthly equivalent effective temperature." The monthly values then may be combined into seasonal and annual equivalent effective temperatures as shown in Table III.

Conductor surface of bare copper ultimately becomes darkened by exposure to the atmosphere. Various investigators have reported surface thermal emissivity factors from 0.15 for bright copper increasing to 0.77 for well-tarnished copper. In industrial districts, the formation of a tarnish film is accelerated by atmospheric smoke and other waste products and a coating develops which is judged to have an emissivity slightly higher than the ordinary tarnished surface. As a compromise, it is proposed to use 0.50 in subsequent calculations. This value would permit slight overheating of new conductors, which would tend to accelerate oxidation and would be compensated amply by underheating after the tarnished surface had formed completely.

Conductor outside diameter enters into computations of convection and radiation, both of which are dependent upon the outside surface area. For solid conductors, the surface is obviously π (diameter) (length). Stranded conductors have an irregular surface which is considered equivalent to an envelope measured as π (diameter over strands) (length).

With the requisite data thus established, it is possible now to compute the dissipation of heat at various conductor temperatures. During the last forty years, many experimenters have published reports about the dissipation of heat from cylindrical surfaces in air, but I. Langmuir[4] and C. W. Rice[5,6] seem to have prepared the most complete presentations.

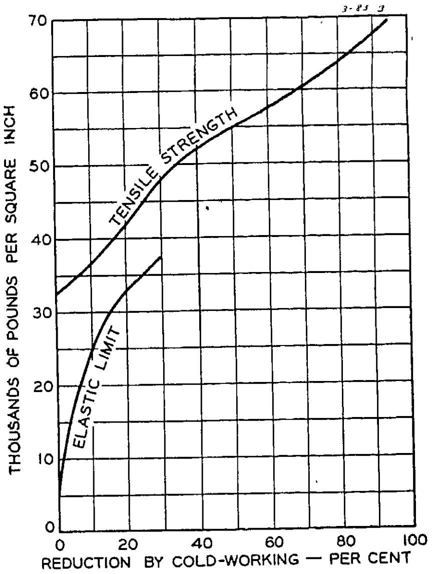


Figure 3. Tensile strength and elastic limit of cold-drawn copper

Total dissipation of heat,

$$W = W_R + W_C \text{ watts per centimeter of length} \qquad (1)$$

where radiation,

$$W_R = 18.55\times10^{-12}D\,[(T_2)^4-(T_1)]E \text{ watts per centimeter of length} \qquad (2)$$

and convection,

$$W_C = S(\phi_2-\phi_1) \text{ watts per centimeter of length} \qquad (3)$$

The shape conductance, S, of the stationary film of air held by friction to the surface of the conductor is determined as

$$S = 2\pi/\log_e\left(\frac{2B+D}{D}\right) \qquad (4)$$

where the film thickness for moving air

$$B = (0.00572D^{0.5}T_a^{0.877})/(P^{0.5}V^{0.5}) \text{ centimeter} \qquad (5)$$

The thermal conductivity of the air film

$$(\phi_2-\phi_1) = 3.46\times10^{-6}T_a^{0.754}(T_2-T_1) \qquad (6)$$

In equations 1 to 6, the symbols are defined as follows:

T_2 = absolute temperature of conductor, degrees absolute
T_1 = absolute temperature of atmosphere, degrees absolute
T_a = average of conductor and atmospheric temperatures, degrees absolute
ϕ_2 = thermal conductivity of air at atmospheric pressure and temperature T_2
ϕ_1 = thermal conductivity of air at atmospheric pressure and temperature T_1
E = thermal emissivity factor for surface of conductor (black body = 1.00)
D = outside diameter of conductor, centimeters
P = atmospheric pressure, atmospheres
V = wind velocity, centimeters per second

Subsequent tests by Schurig and Frick[3] confirmed the aforementioned relationships within close limits for conductor sizes from 0.46-inch diameter up.

The next step is to utilize these data and equations to compute the energy which can be dissipated from the conductor. A typical calculation for 4/0 stranded is given as Appendix A, with the annual equivalent effective temperature (including solar heating) substituted for atmospheric temperature. Similar calculations for other conductor temperatures permit drawing the curves for the dissipation of heat from the 4/0 bare stranded conductor shown in Figure 2.

Knowing the conductor temperature, it is necessary now to determine the conductor resistance. The resistance of 4/0 stranded hard-drawn copper is given as 0.0624 ohm per 1,000 feet at 75 degrees centigrade and the temperature coefficient is 0.00408 per degree centigrade. Hence the resistance at any other temperature is found from the relationship

$$R = 0.0624(1+0.00408t_2)/(1+0.00408\times75) \text{ ohms per 1,000 feet} \qquad (7)$$

where t_2 = conductor temperature, degrees centigrade. For convenience, the resistance at various temperatures has been added to Figure 2.

With the total watts dissipation and the resistance both known for various conductor temperatures, the corresponding conductor current is determined as

$$I = \sqrt{W/R} \text{ amperes} \qquad (8)$$

Various points give the heavy curve of Figure 2 for the relationship between current and conductor temperatures in air at 76.0 degrees Fahrenheit (24.5 degrees centigrade) with sufficient solar heating to raise the effective conductor temperature 3.1 degrees Fahrenheit (1.7 degrees centigrade) and wind velocity of 10.5 miles per hour at the airport.

The family of curves for other values of temperature and wind velocity can be derived readily from Figure 2 by modification of the method proposed by Kidder and Woodward.[7] Their equation 13 is modified for bare wire to

$$I = CD^{1.25}(t_2-t_1)^{0.5}V^{0.25} \qquad (9)$$

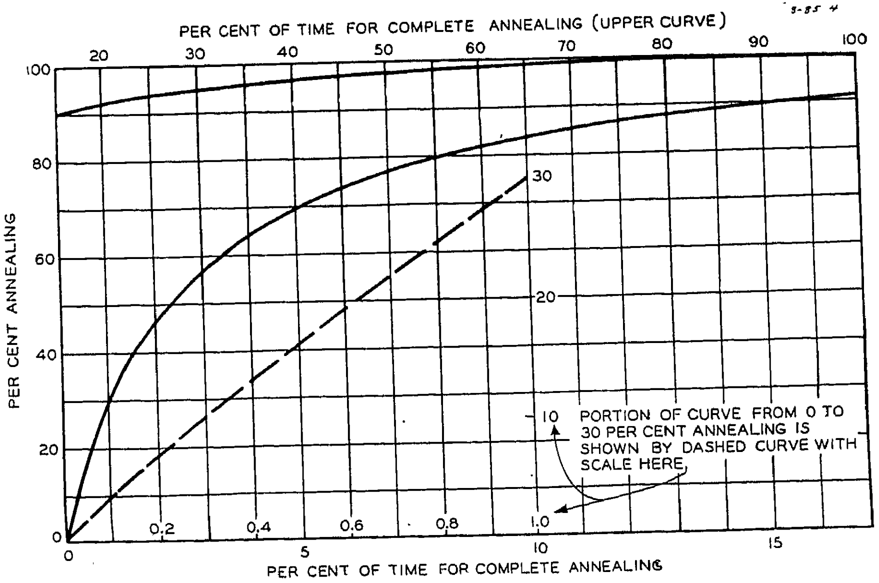
In this equation for a given wire size $CD^{1.25}$ is substantially constant throughout the range of temperatures and wind velocities required, therefore we may write

$$I' = I(t_2'-t_1')^{0.5}(V')^{0.25}/(t_2-t_1)^{0.5}(V)^{0.25} \text{ amperes} \qquad (10)$$

where

I = current from Figure 2, amperes.
I' = current for other atmospheric conditions, amperes
t_2 = conductor temperature from figure two, degrees centigrade
t_2' = conductor temperature for other atmospheric conditions, degrees centigrade
t_1 = annual equivalent effective ambient temperature = 26.2 degrees centigrade
t_2 = other equivalent effective annual temperature, degrees centigrade
V = annual average wind velocity at Weather Bureau observation post = 10.5 miles per hour

Figure 4. Annealing rate of cold-worked electrolytic copper



V = other wind velocity at Weather Bureau observation post, miles per hour

With this relationship established, multiplying factors to convert the current curve of Figure 2 computed on the annual equivalent effective basis to corresponding current curves at the same conductor temperatures for summer and winter are determined as follows:

	Annual	Summer (May–September)	Winter (November–January)
t_2 =	56.0 C		
t_2' =		56.0 C	56.0 C
t_1 =	79.1 F = 26.2 C		
t_1' =		88 7 F = 31.5 C	60.5 F = 15.8 C
V =	10.5 miles per hour		
V' =		9.0 miles per hour	11.8 miles per hour
I =		510 amperes	
I' =		$510\frac{(24.5)^{0.5}(9.0)^{0.25}}{(29.8)^{0.5}(10.5)^{0.25}}$ = 444 amperes	$510\frac{(40\ 2)^{0.5}(11.8)^{0.25}}{(29.8)^{0.5}(10.5)^{0.25}}$ = 611 amperes

Thus we have a method to correlate conductor temperature and current with atmospheric temperature and pressure, solar heating, and wind velocity for any period of the year.

Annealing Characteristics of Copper Conductors at Low Temperatures

An offhand figure for the annealing temperature of copper often is given as 200 degrees centigrade. Investigation of the literature on the subject, however, discloses a number of notations of copper being annealed at temperatures below 100 degrees centigrade. The rate of annealing at these low temperatures depends upon the temperature, the time for which that temperature is maintained, the purity of the copper (small fractions of one per cent of silver or arsenic have marked effect), and the amount of cold-working in the process by which the copper was prepared.

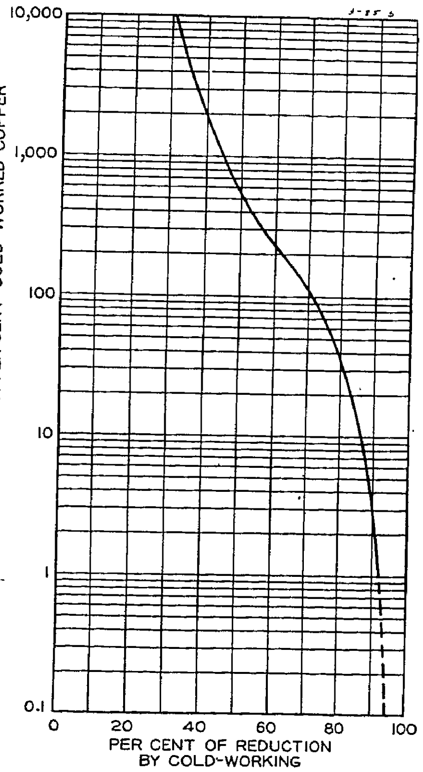


Figure 5. Effect of percentage of cold-working upon the annealing time of electrolytic copper

Wire is manufactured to meet the American Society for Testing Materials specifications which measure purity in terms of ability to meet mechanical and electrical requirements. Since it is known that pure copper has the lowest annealing temperature and electrolytic copper is probably the purest grade used in the manufacture of commercial wire, it is proposed to base all annealing considerations upon commercial electrolytic copper.

The amount of cold-working in solid copper transmission conductors is indicated by the tensile strength. Stranded conductors are rated as cable, usually running approximately 90 per cent of the summation of strength of the individual strands, whence determination of the characteristics of the copper must be based upon the individual strands. Comparison of the specifications for conductors in common use indicates rather consistent ranges of tensile strength.

Conductor Size (B and S Gauge)	Strands: Number	Strands: Size (Inch)	Tensile Strength (Pounds Per Square Inch): Conductor	Tensile Strength (Pounds Per Square Inch): Strands
Hard Drawn				
4/0	7	0.1739	55,100	61,200 to 62,000
1/0	7	0.1228	57,300	63,700 to 64,300
3	1	0.2294	59,000	59,000
Medium Hard Drawn				
4/0	7	0.1739	45,600	48,660 to 56,000
1/0	7	0.1228	48,500	49,660 to 57,000
3	1	0.2294	51,500	48,000 to 55,000

The hard-drawn copper ranges from 59,000 to 64,300 pounds per square inch; the medium-hard-drawn from 48,000 to 57,000 pounds per square inch. Because of the change to stranded conductors for the larger sizes, the variation in diameter of the copper is held to a rather close range, 0.1228 to 0.2294 inch. By grouping the conductors in this way, it becomes feasible to derive one set of data for the annealing of hard-drawn conductors and a second set for medium-hard-drawn.

The only extensive investigation of the annealing characteristics of cold-worked copper which has been found is that by Pilling and Halliwell.[8] They prepared samples of four different amounts of cold-rolling from the same batch of commercial electrolytic copper and heated test pieces from these samples for different lengths of time at different temperatures. From these tests, they derived

1. A curve showing the relationship between the amount of cold-rolling and the tensile strength.
2. A curve showing the per cent of annealing against per cent of the time required for complete annealing which applies to widely different conditions.
3. Two curves showing the effect of the amount of cold-rolling upon the time required for complete annealing.
4. Curves showing the time required at various temperatures for complete annealing of copper of two different amounts of cold-rolling.

Chemical analysis of the copper showed oxygen 0.066 per cent, iron 0.003 per cent, sulphur 0.002 per cent as the only measurable impurities.

Equivalent data for hard-drawn copper wire are not available, but limited test data have been reported by various experimeters. Metallurgists advise that there is no great physical difference between cold-drawn and cold-rolled copper; therefore it is reasonable to utilize the Pilling and Halliwell curves to convert the wire test data to a common base of an initial tensile strength of 64,000 pounds per square inch. Data published by Webster, Christie, and Pratt[9] establish a curve for tensile strength versus reduction of area by cold-drawing for electrolytic copper, reproduced as Figure 3, by which 64,000 pounds per square inch tensile strength is found to correspond to 80.5 per cent cold-drawing. This curve, together with the Pilling and Halliwell data showing the per cent of annealing versus per cent of time for complete annealing and the effect of per cent of cold-working upon the annealing time, reproduced here as Figures 4 and 5, respectively, complete the tools for this conversion. The twelve points, after conversion to 80.5 per cent cold-drawing, have been plotted in Figure 6, then averaged by the method of least squares to the solid curve. For comparison, the corresponding curve for cold-

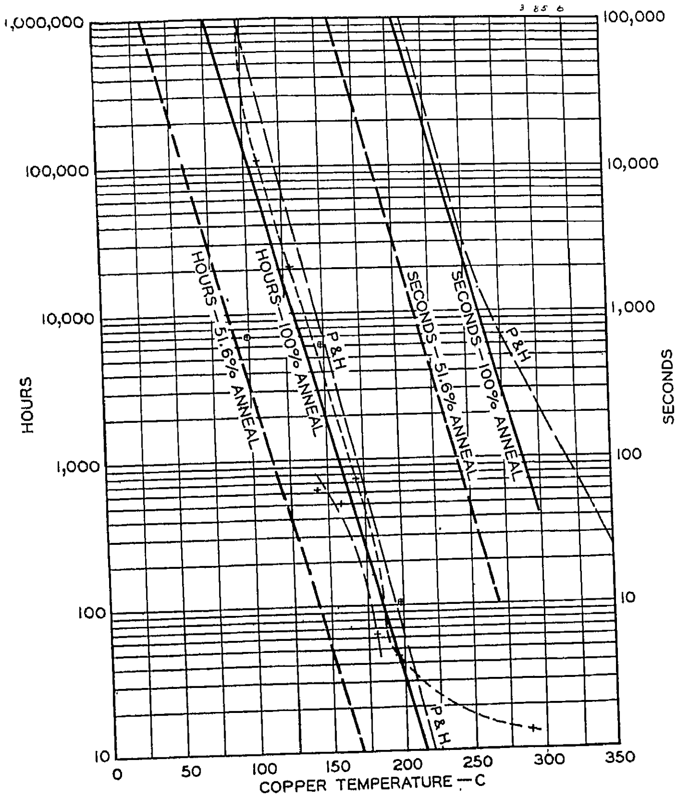


Figure 6. Time required to anneal completely 80.5 per cent cold-drawn copper wire

Heavy dash curves show time permissible to retain 1.5 factor of safety

—— Average for hard-drawn wire

- - - Tests on 0.104-inch hard-drawn wire

— — Tests on 4/0 seven-strand hard-drawn conductor

⊕ ⊕ Points from other tests

P & H Computed from Pilling and Halliwell cold-rolled data

rolled copper has been added. The points show greater deviation than could be considered desirable, but that may be attributed to probable errors in tests run for other purposes in which the annealing data were only incidental, also in part to possible variations in the silver content of the test specimens. It is hoped that carefully controlled tests can be run to establish the time-temperature data for hard-drawn copper transmission conductors definitely in order to make available their full capacity. Until this is done however, the only reasonable procedure is to use such a curve as Figure 6, allowing ample margin for possible error in annealing data.

Similar analysis for medium-hard-drawn copper of 57,000 pounds per square inch tensile strength indicates that the temperature must be approximately 26 degrees higher than for hard-drawn copper to anneal completely in the same time.

Since some engineers are interested in the amount of annealing at a certain temperature for varying lengths of time, or, in the remaining tensile strength, Figure 7 has been derived from the aforementioned data. This illustration shows clearly the rapid loss in strength of hard-drawn copper as compared with medium-hard-drawn and may be useful for comparison of these calculated values with laboratory data.

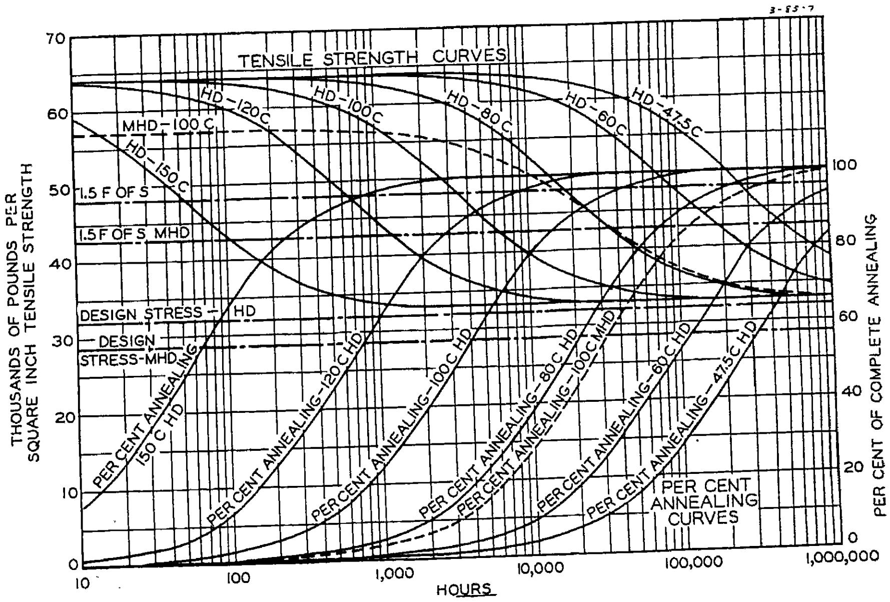
The ultimate strength of completely annealed hard-drawn copper (annealed at low temperature) is about 33,000 pounds per square inch, hence the loss from an initial strength of approximately 64,000 pounds per square inch is only 48 per cent. Since most designs are based on conductors stressed to 50 per cent of ultimate for heavy-loading conditions, it is probable that sufficient margin would remain to keep the conductor from failing. The softened conductor has a very low elastic limit, however, and tends to stretch until the resultant stress has been reduced to the new elastic limit. This phenomenon causes the annealed wires to sag lower than originally, and removing the slack does not permanently correct the condition.

In order to maintain approximately the design sags, it is necessary to keep the elastic limit above the design tension. All annealing data are expressed in terms of tensile strength, and there appears to be no direct relationship between the strength and the elastic limit after partial annealing. A curve plotted from limited data for the elastic limit against the per cent of reduction by cold-working has been added to Figure 3 to provide an approximate means for converting the required elastic limit into corresponding ultimate tensile strength. The difference between this and the initial tensile strength is the allowable loss due to annealing which in turn establishes time-temperature limits for the copper conductor.

Hard-drawn stranded conductors have an initial tensile strength in the strands of approximately 64,000 pounds per square inch. Transmission design normally provides a factor of safety of 2.0, stresses the strands to approximately 32,000 pounds per square inch under ice and wind loading conditions. High-temperature operation tends to reduce the tensile strength and must be presumed to lower the elastic limit also. It would seem desirable, therefore, to restrict the temperature and time to values which will preserve an elastic limit not lower than the design tension of 32,000 pounds per square inch and an ultimate tensile strength to give a factor of safety of at least 1.5.

Figure 7. Annealing and loss of tensile strength of hard-drawn copper conductors (HD)

A corresponding curve for medium-hard-drawn copper conductors (MHD) shown by dash line for comparison. Design stress and 1.5 factor of safety limits (F of S) indicated by broken lines



The elastic limit of copper is indefinite at best, and the available data are exceedingly limited. It appears to increase with cold-drawing in much the same manner as the tensile strength, as indicated by the curve added to Figure 3. Since all data available on annealing are measured in terms of tensile strength rather than elastic limit, the only means available for establishing an annealing limit from elastic limit is in terms of the corresponding tensile strength. Assuming that the elastic limit of hard-drawn copper annealed down to a medium-hard-drawn tensile strength is similar to that of copper drawn directly to the same medium-hard-drawn tensile strength, we find that an elastic limit of 32,000 pounds per square inch requires a tensile strength of 41,000 pounds per square inch. This strength gives a factor of safety of only 1.28, however, and the limitation of annealing is established by the desired 1.5 factor of safety at a minimum tensile strength of 48,000 pounds per square inch. The permissible annealing thus is

$$(64{,}000-48{,}000)/(64{,}000-33{,}000)=51.6 \text{ per cent}$$

Reference to Figure 4 indicates that 51.6 per cent annealing occurs in 2.47 per cent of the time for complete annealing. Accordingly a dashed curve has been added to Figure 6 showing the time-temperature limits of cold-drawn copper as established by annealing. It is interesting to note that 100 degrees centigrade for only 3,500 hours terminates the entire useful life of the conductor, whereas 75 degrees centigrade extends the life to 27,000 hours.

With the allowable time-temperature limits established, and a method developed to correlate current with temperature, it becomes possible to set up a basis for operating current ratings. It is intended to provide for 20 years of service before it becomes necessary to replace the conductors because of loss of strength by annealing.

Before operating ratings can be investigated, it is necessary to determine how much annealing is caused by short-circuit currents. Records for a 280-mile 66-kv system show an average of 1.65 faults per line per year, of which over 50 per cent were single-phase-to-ground drawing less than 1,000 amperes. Assuming that the long-time average in excess of 1,000 amperes will be 1.00 faults per year, it becomes possible to determine how much annealing will occur from faults during the 20-year life.

Analysis of the clearing-time-current curves for 4/0 conductors indicates that a curve allowing 5,000 amperes for one second should provide ample opportunity for fault clearing. For currents of such short duration, dissipation of heat into the atmosphere can be ignored, and the temperature rise computed entirely on the basis of thermal capacity. The specific heat of copper averages approximately 180 watt-seconds per pound per degree centigrade, whence 4/0 conductors weighing 640 pounds per 1,000 feet will absorb 115,000 watt-seconds per 1,000 feet per degree centigrade. This energy is supplied by the I^2R loss, which permits setting up the equation

$$I^2R_at=115{,}000(T_1'-T_1) \tag{11}$$

where

R_a = resistance of conductor per 1,000 feet at average temperature

T_1 = initial conductor temperature, degrees absolute

T_1' = final conductor temperature, degrees absolute

t = time in seconds

The resistance, R_a, at average temperature may be represented approximately by the expression

$$R_a=8.95\times10^{-6}(T_1'+T_1) \tag{12}$$

which is substituted in equation 11, rearranged to give

$$T_1'=T_1(12.86\times10^8+I^2t)/(12.86\times10^8-I^2t) \tag{13}$$

Assuming that the initial temperature of conductor was 100 degrees centigrade = 373 degrees absolute, then 5,000 amperes for 1.0 second would give

$$T_1'=388 \text{ degrees absolute}=115 \text{ degrees centigrade}$$

A straight line increase from 100 degrees centigrade to 115 degrees centigrade in one second is approximately equivalent to 109 degrees centigrade throughout the one second. From Figure 6 it is seen that a temperature of 109 degrees centigrade will use up the allowable annealing of hard-drawn conductors in 1,600 hours. Since the total time at 109 degrees centigrade from short-circuit currents in 20 years is only 20 seconds, the annealing in this case is negligible. It is well to note, however, that lines having higher fault experience and slower clearing time-current characteristics very easily could be subject to partial annealing from short-circuit currents. Annealing thus becomes an additional argument for improved protection against faults and better relay protection of circuits requiring high load current ratings.

In this case, the entire allowable annealing is available for apportioning between normal recurrent loading and nonrecurrent emergency loading. Obviously certain lines are more useful with a high nonrecurrent rating and relatively low normal rating, while other lines require a relatively high normal rating and very little or no additional capacity for nonrecurrent loading. For this analysis, the allowable annealing has been apportioned two-thirds to normal loading and one-third to nonrecurrent, which factors may be applied directly to the time scale of the 51.6 per cent annealing curve of Figure 6.

For present purposes, it is convenient to work on the basis of continuous loading throughout the 20 years at 100 per cent annual load factor. Data thus will be made available for the worst possible condition to which correction factors may be applied to secure corresponding ratings for the load factor on any particular circuit. Lesser load factors can be converted into equivalent hours at maximum temperature, taking into consideration the coincident atmospheric temperature, solar heating, and wind velocity. The method used by Sealey[2] to convert atmospheric temperature data into equivalent ambient temperatures could be adapted to the conversion of load factor, but for the present it will be assumed that the equivalent time may be expressed as a percentage identical with the load factor. Taking into consideration the usual daily load factors, the variations in peak from day to day, and the shifting of load and of generating schedules over a period of 20 years, it is not likely that the load factor during 20 years will exceed 50 per cent based upon the highest load with the system operating normally, which will be represented as 50 per cent equivalent time.

Computation of Ratings

The economic life of transmission conductors has been assumed to be 20 years. It should be perfectly possible to set up an economic comparison of total annual cost per kilowatt-hour transmitted versus resulting life of conductors and so determine a more exact figure for the economic life. For present purposes, however, a 20-year life is a marked reduction below the life span previously taken for granted and will be used to establish ratings.

Twenty years constitute 175,200 hours, which is to be two thirds of the total allowable annealing time. From Figure 6, the annual equivalent copper temperature at which the total allowable annealing occurs in $^3/_2\times175{,}200$ hours (262,500 hours) is found to be 47.5 degrees centigrade. Referring to Figure 2, the conductor current corresponding to 47.5 degrees centigrade is found to be 435 amperes. This value is the maximum current which could be carried continuously for 20 years in a 4/0 hard-drawn bare copper transmission conductor fully exposed to atmospheric temperature and winds without dangerously annealing the copper.

Nonrecurrent emergency loading must be restricted to nonprearranged operating conditions not to occur more frequently than possibly five times per year if the rating is to be sufficiently higher to be of value. A convenient duration to provide time for most emergency repairs seems to be 24 hours per nonrecurrent load period. With 100 such load periods during the twenty-year life, a total of 2,400 hours must be assumed, which constitute one third of the total annealing time. From Figure 6, the annual equivalent copper temperature at which the total allowable annealing occurs in three times 2,400 hours (7,200 hours) is found to be 91 degrees centigrade, and Figure 2 shows a corresponding current of 720 amperes.

The conductor temperatures thus far have been expressed on an equivalent annual basis, assuming atmospheric temperature based upon monthly means, average wind velocity, and less than the maximum solar heating. Some indication of the amount of deviation above and below these equivalent average temperatures is given as follows:

	Conductor Temperature		
	Equivalent Annual	July 1942 (High Hour)	December 1942 (Low Hour)
Current, 100 per cent load factor amperes	435	435	435
Atmospheric temperature, degrees Fahrenheit	76.0	78.0	7.0
Solar heating, degrees Fahrenheit	3.1	0.0	0.0
Wind velocity, miles per hour	8.4*	1.0	26.0
Conductor temperature, degrees Fahrenheit	117.5	190.0	29.0
Conductor temperature, degrees centigrade	47.5	87.6	−1.7

* Squared average root equivalent at Pittsburgh of 10.5 miles per hour average at airport.

Dropping back to the 50 per cent 20-year load factor, the allowable temperatures would be these shown on Figure 6 corresponding to 50 per cent of the aggregate time for 100 per cent load factor. The annual equivalent copper temperature for normal operation is found to be 56.0 degrees centigrade corresponding to 510 amperes; the annual equivalent copper temperature for nonrecurrent loading is 99.5 degrees centigrade, corresponding to 760 amperes. With these ratings established on an annual equivalent basis, they may be modified by means of equation 10 to any desired seasonal or monthly condition.

As long as the conductor temperature does not exceed approximately 120 degrees centigrade, it is not likely that the contact surfaces in connectors and splices will become hot enough to cause rapid oxidation and consequent damage to the joint. This temperature limitation should be maximum, however, not annual equivalent, and it would seem advisable to use a somewhat lower value on the order of 100 degrees centigrade unless a detailed hour-by-hour analysis of weather data and load currents is to be conducted.

An earlier section of this paper has mentioned several reasons why transmission conductors cannot be assumed to be always in perfect condition. In addition, the entire discussion of weather data has been directed toward deriving a set of indexes to represent in one set of figures the composite effect of many variables. Finally the information available by which to incorporate annealing of the hard-drawn copper conductor in this analysis has required many approximations. In view of all these possibilities for error, it must be urged that the ratings thus derived be applied with a generous margin of safety until further investigation of the points now inadequately covered by test data permits more accurate analyses.

It is recommended for the present that the calculated ratings should be derated at least 20 per cent. Any circuits so rated should be set up for close observation of actual load and coincident atmospheric conditions with periodic tensile and elastic limit tests on samples removed from service at definite intervals of time as a check upon the validity of the calculations and as an indication of the time when the conductors should be replaced. Only by such procedure is it likely that thoroughly dependable information will become available to the industry.

Appendix

Calculation of heat dissipation from 4/0 bare conductor at 100 degrees centigrade into atmosphere at 26.2 degrees centigrade equivalent effective temperature with wind velocity of 10.5 miles per hour:

$T_2 = 100\text{ C} = 373\text{ K}$

$T_1 = 79.1\text{ F} = 26.2\text{ C} = 299.2\text{ K}$

$$T_a = \frac{T_2 + T_1}{2} = \frac{672.2}{2} = 336.1\text{ K}$$

$E = 0.50$

$D = 0.528\text{ in.} = 1.340\text{ cm}$

$P = 0.96$ atmosphere

$V = 40.5\%$ of $10.5 = 4.25$ mph $= 190$ cm per sec

$$W_R = 18.55 \times 10^{-12} \times 1.340[(373)^4 - (299.2)^4]0.5$$

$= 0.142$ watt per cm length $= 4{,}330$ watts per 1,000 ft

$T_a^{0.754} = \log_{10}^{-1}(0.754 \times 2.527) = 79.8$

$(\phi_2 - \phi_1) = 3.46 \times 10^{-6} \times 79.8)(73.8) = 0.0204$

$D^{0.50} = 1.16 \quad p^{0.50} = 0.98 \quad V^{0.50} = 13.8$

$T_a^{0.877} = \log_{10}^{-1}(0.877 \times 2.527) = 165.0$

$$B = \frac{0.00572 \times 1.16 \times 1{,}650}{0.98 \times 13.8} = 0.081\text{ cm}$$

$$S = \frac{2\pi}{\log_e (1.121)} = \frac{2\pi}{0.114} = 55.1$$

$W_c = 55.5(0.0204) = 1.130$ watts per cm length $= 34{,}450$ watts per 1,000 ft

$W = 1.272$ watts per cm, $= 38{,}780$ watts per 1,000 ft

References

1. EFFECT OF MAGNETIC HEATING ON CABLES W. H. Burleson. *Electrical World*, March 20, 1943, pages 100–02.

2. EQUIVALENT AMBIENT TEMPERATURES FOR LOADING TRANSFORMERS, W. C. Sealey. AIEE TRANSACTIONS, volume 62, 1943, February section, pages 87–93.

3. HEATING AND CURRENT-CARRYING CAPACITY OF BARE CONDUCTORS FOR OUTDOOR SERVICE, O. R. Schurig, C. W. Frick. *General Electric Review*, March 1930, pages 141–57.

4. CONVECTION AND RADIATION OF HEAT, I. Langmuir. American Electrochemical Society *Transactions*, volume 23, 1913, pages 299–332.

5. FREE AND FORCED CONVECTION OF HEAT IN GASES AND LIQUIDS, C. W. Rice. AIEE TRANSACTIONS, volume 42, 1923, pages 653–706.

6. FREE CONVECTION OF HEAT IN GASES AND LIQUIDS—II, C. W. Rice. AIEE TRANSACTIONS, volume 43, 1924, pages 131–43.

7. AMPERE LOAD LIMITS FOR COPPER IN OVERHEAD LINES, A. H. Kidder, C. B. Woodward. AIEE TRANSACTIONS, volume 62, 1943, March section, pages 148–52.

8. SOFTENING OF HARD-ROLLED ELECTROLYTIC COPPER, N. B. Pilling, G. P. Halliwell. American Society for Testing Materials *Proceedings*, 1925, part 2, pages 97–119.

9. COMPARATIVE PROPERTIES OF OXYGEN-FREE HIGH-CONDUCTIVITY PHOSPHORIZED AND TOUGH-PITCH COPPERS, Webster, Christie, Pratt. American Institute of Mining and Metallurgical Engineers *Proceedings*, 1927, page 233.

Interim Report on Characteristics and Performance of Conductors for Supervisory Control and Telemetering

AIEE SUBCOMMITTEE ON CHARACTERISTICS AND PERFORMANCE OF CONDUCTORS FOR SUPERVISORY CONTROL AND TELEMETERING

Preface: The present war emergency requires that the maximum use be made of existing equipment and systems and that a minimum of critical material be used for new equipment.

This publication and other guides and reports of this series have been prepared for the information of users during the war emergency. Upon termination of the war emergency, they will be reconsidered by the Standards committee and the committees which prepared them, and will be approved, revised for normal use, or rescinded.

This procedure is being followed in preference to the preparation of special emergency Standards which might involve redesigning and drastic changes in manufacturing practices. These guides will accomplish the maximum conservation of critical materials, since they provide for the maximum use of existing equipment and systems, as well as new equipment, without changing the fundamental basis on which the present Standards have been prepared.

DURING 1941, in collaboration with the relay subcommittee of the committee on protective devices,[*] a questionnaire with regard to pilot conductors[**] for supervisory control and telemetering systems, was sent to a number of operating companies by the committee on automatic stations.

The number of reporting companies or governmental units (25) represents but a small portion of those to whom the questionnaire was sent, and the summary may not, therefore, represent completely the practice and experience of the majority of those in the electric-power industry using pilot conductors for supervisory control and telemetering. It is believed, however, that within its limitations as a sample, it is indicative of certain features and is being presented with this view in mind.

Summary of Data

The summary is based on the data covering 189 circuits totaling slightly under 1,200 circuit-miles and slightly over 9,000 circuit-mile-years. The generalizations permissible from the data are:

(*a*). Of the 189 circuits of an average length[†] of 6.3 miles—only 15 per cent being over ten miles in length[†]—about 55 per cent utilized privately owned pilot conductors. Of 75 of these privately owned pilot conductors, 84 per cent were close to power circuits. Of all the circuits—both privately owned and leased—for which data were received, less than 15 per cent used open-wire construction for even a part of the circuit. Only about ten per cent used a ground-return path for any portion of the circuit.

(*b*). Considering the performance of the pilot conductors alone, the out-of-service time is not over $\frac{1}{20}$th of one per cent of the total time. While this figure is significant, data are not available showing what part of the *total out-of-service* time of the supervisory-control or telemetering systems may be due to the pilot conductors.

(*c*). It may be noted—see section VII of the summary—that two bases for summarizing the service performance have been used. Basis *A* indicates that the leased pilot conductors had about double the outages per circuit-mile-year of the privately owned conductors. The average duration of an outage for the leased conductors was slightly less than that for the privately owned conductors. Basis *B* indicates that the leased conductors had about one-half the outages per circuit-mile-year of the privately owned conductors and a slightly longer average duration for an outage. Considering both bases for summarizing, it appears that there is no substantial difference between the performances of privately owned and of leased pilot conductors. There is also indication that:

1. There is a continuing improvement in the degree of service continuity for pilot conductors. Most of the troubles for the systems occurred during their initial periods and, in some instances involving the older systems, these troubles largely occurred ten years or more ago.

2. This improvement appears to be due **largely** to an increased use of cable conductors **and** to more effectively designed and co-ordinated terminal protection.

(*d*). The special information covered in section VIII of the summary indicates that for new pilot conductors:

1. There is a preference for automatic supervision.
2. Cable conductors **are** desired.
3. The protective terminal equipment is more frequently connected to the power station ground than (*a*) to a remote ground or (*b*) left ungrounded.

Detailed Summary of Answers to Questionnaire

I. GENERAL

(*a*). Number of companies and governmental units submitting answers........ 25
Number of circuits for which some data were provided........................189

(*b*). Uses for circuits

1. Circuits for supervisory control and/or indication.................................. 80
2. Circuits for telemetering.................. 31
3. Circuits for both........................ 13
4. Circuits for systems not identified......... 65

(*c*). Ownership of circuits (question 1 of questionnaire of March 12, 1941—see appendix)

1. Privately owned throughout..............106
2. Leased throughout...................... 81
3. Partially leased and partially privately owned.................................... 2

(*d*). Type of construction (question 2)

1. Circuits wholly of cable construction.......163
2. Circuits wholly of open-wire construction.. 17
3. Circuits of both cable and open-wire........ 8
4. Not indicated.......................... 1

(*e*). Proximity of supervisory or indicating conductors to power circuits (question 3)

1. Privately owned
   Near.....63 Distant.... 12
2. Leased
   Near...5 Distant...65 Both.... 3
3. No information......................... 41

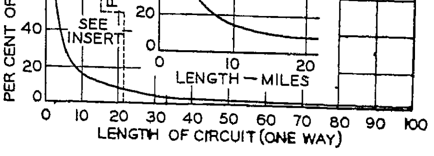


Figure 1. Cumulative per cent curve of circuits versus length

Paper 43-101, recommended by the AIEE committee on automatic stations for presentation at the AIEE national technical meeting, June 21–25, 1943. Manuscript submitted April 21, 1943; made available for printing May 19, 1943.

Personnel of subcommittee on characteristics and performance of conductors for supervisory control and telemetering: R. F. Davis, *chairman;* M. E. Reagan, J. R. Harrington, Perry Peterson.

This interim report was prepared by the AIEE subcommittee on characteristics and performance of conductors for supervisory control and telemetering of the committee on automatic stations for the purpose of making essential information immediately available to war industries, thus furthering the conservation of valuable material for the war emergency. It is educational and in no way mandatory. It is not intended as a "Standard" and has not been formally approved by the Standards committee nor the board of directors.

The committee on automatic stations expresses its appreciation to the 25 companies and governmental units which furnished the data in the "Detailed Summary of Answers to Questionnaire."

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[*] The results of the questionnaire with regard to pilot-wire circuits used for protective relaying were covered in a report of the committee on protective devices which was presented at the AIEE national technical meeting, January 25–29, 1943.[1]

[**] The term, "pilot conductors," as used in this report should not be confused with conductors used for protective-relaying systems, even though the conductor characteristics may be quite similar.

[†] In the case of the 174 circuits for which the length was stated.

(*f*). Type of channel used (question 4)

1.	Completely metallic	168
2.	Ground-return	9
3.	Operating on phantom*	11
4.	Part metallic and part ground-return	1

II. Characteristics of Privately Owned Conductors

(*a*). Size of conductor—number of circuits of various gauges (question 5)

Number 8 copper or copper-weld	*Number 12 copper*	*Number 14 copper*	*Number 16 copper*	*Number 19 copper*	*Number 22*
11	42	4	12	21	8

Miscellaneous (not over 2 of any gauge)	*Not given*
8	2

(*b*). Type of insulation—number of circuits having various insulating material (question 6)

Rubber	*Paper*	*Open-wire (bare)*	*Not given*
47	39	21	1

(*c*). Size of cable—number of circuits involved in cables which have (question 7)

1–5 conductors	*6–10 conductors*	*11-20 conductors*	*21-50 conductors*
10	35	19	11

51–100 conductors	*101–202 conductors*	*Not given*
7	9	1

(*d*). Arrangement of conductors in cables—number of circuits where conductors were (question 8)

Nontwisted	*Twisted*	*Quadded*	*Not given (or indefinite)*
39	34	5	6

(*e*). Type of sheath—number of circuits in cables having (question 9)

Lead sheath	*Nonmetallic sheath*	*Not given*
79	5	1

III. Length, Resistance, and Loading of Conductors

(*a*). Length (question 10—see Figure 1)

Average of 174 circuits for which length was given............................6.3 miles
Range: Minimum of 0.28 mile to maximum of 112.9 miles

(*b*). Resistance (question 11—see Figure 2)

Average of 115 circuits for which resistance was given............................475 ohms
Range: Minimum of 6.5 ohms to maximum of 4,000 ohms

(*c*). Loaded or nonloaded (question 12)

149 circuits nonloaded
21 circuits loaded
2 circuits partly loaded and nonloaded
17 circuits no information given

IV. Protection at Terminal of Circuit

(*a*). Number of circuits indicated as having the following protective devices (question 13):

Carbon blocks	65
Air gaps	6
Thyrite	4
Protective tubes	55
Vacuum gaps	8
Fuses	101
Heat coils	5
Relay protectors	7
Other devices	9

Circuits indicated as having

One type of protective device	66
Two types of protective devices	55
Three types of protective devices	25
Four types of protective devices	2

(*b*). Circuits indicated as having no protective devices.......................... 5

(*c*). Circuits for which no information was given............................36

IV*A*. Breakdown Voltage of Protective Devices Summarized in Item IV(a)

(*a*). At 60 cycles (question 14*a*)

*0–100 volts**	*101–250 volts**	*251–350 volts**	*351–600 volts**	*Not given*
9	15	22	21	122

(*b*). Impulse voltages (question 14*b*)

No information furnished

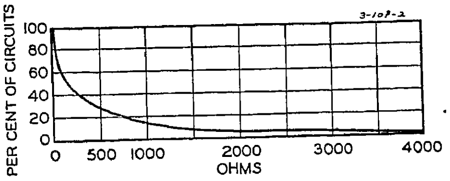

Figure 2. Cumulative per cent curve of circuits versus loop resistance of conductors

IV*B*. Protection Against Potential Gradients (Question 15)

Neutralizing transformers	4 circuits
Insulating transformers*	6(1) circuits
Insulating relay equipment	31 circuits
Drainage**	16 (3) circuits
Relay Protectors**	(2) circuits
Series impedance	2 circuits
No special protection	37 circuits
No information given	94 circuits

IV*C*. Insulation Level of Pilot Wires (Question 16)

0–100 volts	*101–499 volts*	*500 volts*	*1,500 volts*
7 circuits	2 circuits	54 circuits	5 circuits

3,500 volts	*15,000 volts*	*No data given*
1 circuit	2 circuits	118 circuits

This is assumed to be the dielectric strength (conductor-to-ground), as determined by manufacturers' tests, which is usually in rms volts.

V. Protection on Pilot Wires at Central Offices and Other Remote Points from Terminal of Circuit (Question 17)

(*a*). 55 circuits equipped with carbon blocks
(*b*). 1 circuit equipped with air gaps
(*c*). 6 circuits equipped with protector tubes
(*d*). 29 circuits equipped with fuses
(*e*). 8 circuits equipped with heat coils
(*f*). 1 circuit equipped with relay protectors
(*g*). 8 circuits having no protection
(*h*). 23 circuits having two of above devices
(*i*). 10 circuits having three of above devices
(*j*). 124 circuits for which no data was given

V*A*. Breakdown Voltage of Protective Devices Summarized in item V

(*a*). At 60 cycles (question 18*a*)

0–200 volts	*201–400 volts**	*401–600 volts**	*No data given*
7 circuits	21 circuits	15 circuits	146 circuits

(*b*). Impulse voltages (question 18*b*)

No information furnished.

VI. Special Markings or Other Precautionary Measures Taken to Prevent Inadvertently Interrupting Pilot Wires (Question 19)

(*a*) *Circuits with special markings*	*Circuits having no special markings*
74	18

(*b*) Circuits having other special features

1 circuit having special routing
1 circuit having notification before testing

(*c*) *Circuits for which no data were given*
95

VII. Performance of Pilot-Wire Conductors

(*a*). General (questions 10, 20, 21 and 22—see Table II)

Two bases for summarizing have been used. Basis *A* consists of averaging all of the data furnished, even though various elements of the information do not apply to the same circuit, for example, for "years of service" data were furnished for 71 privately owned circuits and 70 leased circuits, and for "number of outages" the data cover 60 privately owned circuits and 59 leased circuits but *not necessarily for the same circuits*. Basis *B* consists of averaging *only* those circuits for which complete data for *all* four of the items of information were furnished.

(*b*). Causes of service outages (question 23)

Total outages for which causes were indicated	475
Due to short circuit of pilot-wire conductors	120 (25.3 per cent)
Due to open circuit of pilot-wire conductors	119 (25.1 per cent)
Due to circuit rearrangement of pilot-wire conductors	32 (6.7 per cent)
Due to operation of terminal protectors on pilot-wire conductors	70 (14.7 per cent)
Due to miscellaneous causes	134 (28.2 per cent)

of which 85 were due to failure of supervisory equipment; 36 were due to low insulation during fogs and floods; and the remaining 13 were from scattered causes

VIII. Miscellaneous Information and Comments

Of the various questions listed under the heading "Additional Information" in the appendix, the data received were adequate or suitable for summarizing for the following items:

(*a*). Are pilot-wire conductors automatically supervised? (question 24)

Yes......43 circuits No......110 circuits

(*b*). Are pilot-wire conductors used for other purposes than solely supervisory control and telemetering?—(question 25)

Yes......15 circuits No......132 circuits

(*c*). Are pilot-wire conductors subject to troubles from induction effects or surges?—(question 26)

Yes......22 circuits No......130 circuits

(*d*). Various comments regarding preferences for new pilot-wire conductors—(questions 27 and 28)

1. No. of companies answering............Maximum of 6

(Text concluded on page 862)

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* Often (10) on ground-return basis.

* Assumed to be peak volts although not specified.

** These protective elements were in some cases—as indicated by the figures in parentheses—combined with one or more other protective elements.

Table I

Designation of Power Company	Type of Circuit or Identification**	Quest. 1		Quest. 2		Quest. 3		Quest. 4			Quest. 5	Quest. 6	Quest. 7	Quest. 8			Quest. 9	Quest. 10	Quest. 11	Quest. 12	
		Leased	Private	Cable	Open-Wire	Near	Distant	Metallic	Grd. Return	Phan.	Size of Cond.	Cond. Insulation	No. of Conds. in Cable	Untwisted	Twisted Pairs	Quadded	Type of Sheath	Length (Mi. One Way)	Res. of Loop Exc. Term. Eq. (Ohms)	Loaded	Nonloaded
Company *A*	Tm		x		x		x	x			#10	O.W.						12	126		x
Company *B*	Tm		x		x	x		x			#6	O.W.		x				4.5	—		x
Company *C*	Both		x	x		x		x			#19	Paper	20	x			Pb	4	164	x	
	Both		x	x		x		x			#19	Paper	20	x			Pb	2	82	x	
	Both		x	x		x		x			#19	Paper	12	x			Pb	5	210	x	
	Both		x	x			x	x			#19	Rubber	12	x			Pb	2.5	103	x	
	Ind		x		x	x		x			#12	Rubber	1	x			Pb	1	8	x	
	Tm		x		x	x		x			#12	Rubber	1	x			Pb	1	8		x
	Both	x		x			x	x												x	
	Ind. & Tm	x		x			x	x												x	
	Ind	x		x			x	x												x	
	Ind	x		x			x	x												x	
	Both	x		x			x	x												x	
	Both	x		x			x	x										1.5	300	x	
	S.c.	x		x			x	x										1.5	300	x	
	Tm	x		x			x	x										1.5	300	x	
	S.c.	x		x			x	x										1.5	300		x
	S.c.	x		x			x	x										1.5	300	x	
	S.c.	x		x			x	x										1.5	300	x	
Company *D*	S.c.		x	x	x	x		x			#8 C.W. 19/22 Cu.	— Rubber	1 2		Trans.		Pb	1.5	30		x
Company *E*	Tm	x		x			x	x									Pb	3			x
	Tm	x		x			x	x									Pb	3			x
Company *F*	Tm	x		x						Ph-g								14.5	3,500		x
	Tm	x		x						Ph-g								13.25	3,300		x
	Tm	x		x						Ph-g								3	700		x
	Tm	x		x						Ph-g								3	700		x
	Tm	x		x						Ph-g								7	1,600		x
	Tm	x		x						Ph-g								6	1,200		x
	Tm	x		x						Ph-g								36.75	4,000		x
	S.c.		31	x				x			#12	$^3/_{64}$ R	7	x			Pb	0.8–4.5 (2.2 avg.)	36.2		x
Company *G*		x		x			x	x										12			
		x		x			x	x										½			
		x	x		x	x (P)	x (L)	x (L)	x (P)									5			
		x		x			x	x										4			
		x		x			x	x										3			
Company *H*		x		x		x		x										2			x
		x		x		x		x										3			x
		x		x		x		x										2			x
		x		x			x	x										4			x
		x		x			x	x										5½			x
		x		x		x		x										6			x
		x		x			x	x										4¾			x
		x		x			x	x										1½			x
Company *I*		x		x			x	x													x
		x		x			x	x													x
		x		x			x	x													x
		x		x			x	x													x
		x		x			x	x													x
		x		x			x	x													x
			x	x		x		x			#12	R	3				R	5.9			x
			x	x		x		x			#12	R	3				R	9.5			x
			x	x		x		x			#12	R	3				R	10.3			x
			x	x		x		x			#12	R	3				R	5.1			x
		x		x			x	x										6.3			x
			x	x	x	x		x			#14	R	3				Pb	2.7			x
Company *J*		x		x	x		x	x										28.5	3,317	22.5	6.0
		x		x			x	x										14.1	1,730	5.2	8.9
		x		x			x	x										2.13	514	0	2.13
Company *K*	S.c.	x		x			x	x										5¼			x
	S.c.	x		x			x	x										4			x
	S.c.	x		x			x	x										1¼			x
	S.c.	x		x			x	x										2.4			x
	S.c.	x		x			x	x										1¼			x
	S.c.	x		x			x	x										0.7			x
	S.c.	x		x			x	x										1¼			x
	S.c.	x		x			x	x										1			x
	S.c.	x		x			x	x										1			x
Company *L*		x		x	x		x	x										8	680		x
Company *M*			x		x		x			x	102 Cu.							112.9			
		x		x			x	x										4	1,459		x
		x			x		x	x			109 Fe.							4	280		
			x		x		x	x			#10 AWG							30	300		
Company *N*			x	x		x		x			#19	Paper	20		x		Pb	1.12	95		x
			x	x		x		x			#19	Paper	20		x		Pb	2.9	247		x
			x	x		x		x			#19	Paper	20		x		Pb	3.55	302		x
			x	x		x		x			#19	Paper	20		x		Pb	7.38	627		x
			x	x		x		x			#19	Paper	20		x		Pb	4.1	349		x
			x	x		x		x			#19	Paper	20		x		Pb	13.6	1,156		x
			x	x		x		x			#19	Paper	20		x		Pb	6.58	560		x
			x	x		x		x			#19	Paper	20		x		Pb	10.5	893		x

S.c.—Supervisory Control. Tm.—Telemetering. Ind.—Indicating or Sup. Ind. * Serjedetours. † Relay protectors.

Table I (Continued)

Designation of Power Company	Type of Circuit or Identification**	Quest. 13 Terminal Protection								Quest. 14 Breakdown		Quest. 15 Potential Grad. Pot.				Quest. 16	Quest. 17 Treatment in Tel. Exchanges or Other Remote Pts.								Quest. 18 Breakdown of Item 17	Quest. 19
		Carbon Blocks	Air Gaps	Thyrite	Protective Tubes	Vacuum Gaps	Fuses	Heat Coils	Relay Protect.	60 Cycles	Impulse	Neutralizing Trans.	Line Coup'g Trans.	Ins. Relay Equipment	Drainage Equipment	Ins. Level of Pilot Wire to Sta. Grd.	Carbon Blocks	Air Gaps	Thyrite	Protective Tubes	Vacuum Gaps	Fuses	Heat Coils	Relay Protect.	60 Cycles Impulse	Special Marking
Company *A*	Tm	x				x				400–600 v				x			x				x				400–600 v	
Company *B*	Tm			x						250 v			x													
Company *C*	Both				x					250 v																
	Both				x					250 v																
	Both				x					250 v																
	Both				x																					
	Ind						x																			Yes
	Tm						x																			Yes
	Both						x																			Yes
	Ind. & Tm						x																			Yes
	Ind						x																			Yes
	Ind						x																			Yes
	Both						x																			Yes
	Both						x																			Yes
	S.c.																									Yes
	Tm						x																			Yes
	S.c.						x																			Yes
	S.c.																									Yes
	S.c.																									Yes
Company *D*	S.c.				x		x			600 v																Special route
Company *E*	Tm	x								350 v																
	Tm	x								350 v																
Company *F*	Tm						x							x		500 v	x									Yes
	Tm						x							x		500 v	x									Yes
	Tm						x							x		500 v	x									Yes
	Tm						x							x		500 v	x									Yes
	Tm						x							x		500 v	x									Yes
	Tm						x							x		500 v	x									Yes
	Tm						x							x		500 v	x									Yes
	S.c.															500 v										
Company *G*		x										x														
		x																								
		x			x										x											
Company *H*		x			x		x									1500										Yes
		x			x		x								x & scr's†	1500										Yes
		x			x		x								& scr's	1500										Yes
		x			x		x																			Yes
		x			x		x																			Yes
		x			x		x									1500										Yes
		x					x																			Yes
		x			x		x									1500										Yes
Company *I*					x		x																			Yes
					x		x																			Yes
					x		x																			Yes
					x		x																			Yes
					x		x																			Yes
					x		x																			Yes
									x																	
									x																	
									x																	
									x																	
					x		x													x		x				Yes
									x																	
Company *J*		x					x							Series mp.			x					x	x			Yes
		x					x							Series imp.			x					x	x		350	Yes
		x					x										x					x	x		350	Yes
Company *K*	S.c.	x	x				x										x	x				x				Yes
	S.c.	x	x				x										x	x				x				Yes
	S.c.	x	x				x										x	x				x				Yes
	S.c.	x	x				x										x	x				x				Yes
	S.c.	x	x				x										x	x				x				Yes
	S.c.	x	x				x										x	x				x				Yes
	S.c.	x	x				x										x	x				x				Yes
	S.c.	x	x				x										x	x				x				Yes
	S.c.	x	x				x										x	x				x				Yes
Company *L*		x			x		x							Nothing special			x					x			350	See note
Company *M*		x					x		x		350		x													
		x									350	x					x								350	
		x					x				350						x								350	No
							x		x		350						x					x		x	350	No
Company *N*					x		x				250			x		500										
					x		x				250			x		500										
					x		x				250			x		500										
					x		x				250			x		550										
					x		x				250			x		500										
					x		x				250			x		500										
					x		x				250			x		500										
					x		x				250			x		500										

S.c.—Supervisory Control. Tm.—Telemetering. Ind.—Indicating or Sup. Ind. * Serjedetours. † Relay protectors.

Table I (Continued)

Designation of Power Company	Type of Circuit or Identification**	Quest. 20	Quest. 21	Quest. 22	Quest. 23 No. of Outages Due to:				Quest. 24	Quest. 25	Quest. 26	Miscellaneous Information as Covered in Questionnaire Under "Additional Information." In General These Data Were Not Suitable for Summarizing
		Years of Service	No. of Outages During Service	Total Outage Time (Hrs.)	Short Circuits	Open Circuits	Circuit Rearrangements	Terminal Protect.	Pilot Wire Automatically Super'd?	Pilot Wire Used for Other?	Troubles Due to Induction or Surges?	
Company *A*	Tm.	3	6	1 day per outage—no need for cont.	2				No	No	No	
Company *B*	Tm.	9										No troubles due to surges.
Company *C*	Both		2	Neg.		2			No	No	No	$63 per pr. per yr. for about 1 mile.
	Both								No	No	No	Privately owned supervisory and control cable is located on same structure and *above* power line. Control cable is lead-covered and protected at ends with protective tubes.
	Both								No	No	No	
	Both								No	No	No	
	Ind. Tm.								Yes	Yes	No	
	Tm.								No	No	No	
	Both								Yes	Yes	No	Protective tubes and fuses preferred.
	Ind. & Tm.		8	Neg.	1	5	2		Yes	No	No	Terminal equipment is ungrounded.
	Ind.		6	Neg.	4	1	1		Yes	No	No	No monitoring current. Audible alarm and supervisory signals on controlled equipment.
	Ind.		13	Neg.	2	8	3		Yes	No	No	
	Both								Yes	Yes	No	
	Both		5	Neg.	3		2		Yes	Yes	No	For new installations—separate routed, u.g. and metallic covering to prevent interference or tampering. Automatic superv.
	S.c.		1	Neg.	1				Yes	No	No	
	Tm.								Yes	No	No	
	S.c.								Yes	No	No	
	S.c.		2	Neg.	2				Yes	No	No	
	S.c.		1	Neg.	1 or 2				Yes	No	No	
Company *D*	S.c.	4	0	0	0	0	0	0	Yes	No	No	Prot. tubes grounded to station grounds. Lightning arrester on o.w. connected to C/W grd. rods. Auto-super. on new.
Company *E*	Tm.	12	Some							No	No	
	Tm.	10								No	No	
Company *F*	Tm.	9					x	x	No	No	No	In city $4.50 for 1st ½ mile plus $1.25 per ½ mile per month.
	Tm.	8		Neg.			x	x	No	No	No	
	Tm.	14		Neg.			x	x	No	No	No	Outside city $3.33 per mile per month.
	Tm.	14		Neg.			x	x	No	No	No	Cap. and ldng. of PW does not affect performance.
	Tm.	14	Norec.	Neg.			x	x	No	No	No	
	Tm.	14	Neg.	Neg.			x	x	No	No	No	Telem. ccts. to sta. grd.
	Tm.	5		Neg.			x	x	No	No	No	New telem. & s.c. circuits should be same as now used. S.c. circuits use more current than permitted over leased facilities.
	S.c...17 old	J			x		x		No	No	No	
Company *G*		2							Yes	No	No	Approx. $2 per route mile.
		2							Yes	No	No	Grded. to sta. grds.
		L=9 P=12	No record	No record					No	Tel.&Sig.	Yes	Prefers leased metallic—wants automatic supervision.
		9							No	No	No	
		6							No	No	No	
Company *H*		13	3	9⅚	1		2		Yes	No	Yes	Approx. $2 per route mile.
		14	17	54½	10	1	1	4	Yes	No	Yes	On opp. side of st. from 24 kv.
		14	19	89⅓	13	2	1	3	Yes	No	Yes	C and ldg. does not affect.
		14	10	75⅓	4	4	1	1	Yes	No	Yes	Remote grd. in some cases.
		10	2	6	1	1			Yes	No	Yes	Grds. on PW are detected.
		13	12	108	4	5	1	2	Yes	No	Yes	Inst. new prot. tubes and relay protectors.
		13	2	6	1	1			Yes	No	Yes	
		13	9	35	2	4	1	2	Yes	No	Yes	Auto. super. for new.
Company *I*		5	5	2½					Yes	No	No	$.75 per ¼ mile air line.
		5	5	2½					Yes	No	No	
		5	5	2½					Yes	No	No	
		5	5	2½					Yes	No	No	
		5	5	2½					Yes	No	No	
		5	5	2½					Yes	No	No	
		4	1	5		1			Yes	No	No	
		4	1	5		1			Yes	No	No	
		4	0	0					Yes	No	No	
		2	0	0					Yes	No	No	
		2			1				Yes	No	No	
		2	0	0					No	No	Yes	
Company *J*		9	26	268	14	10	2	0	Yes	No	Yes	$4 per mile per month shortest wire route.
		3	1	1½	0	1	0	0	Yes	No	No	
		1	0	0	0	0	0	0	Yes	No	No	On new installations prefer leased circuits for shorter distances and power line carrier for longer routes.
Company *K*	S.c.	1	1	2	1				No	No	No	
	S.c.	1½	0	0					No	No	No	$4 per mile.
	S.c.	1½	0	0					No	No	No	
	S.c.	3	0	0					No	No	No	
	S.c.	1½	0	0					No	No	No	
	S.c.	3	0	0					No	No	No	
	S.c.	2	0	0					No	No	No	
	S.c.	3¼	0	0					No	No	No	
	S.c.	3¼	0	0					No	No	No	
Company *L*		3¾		4–5 days	70% (ins.)	10%		20%				Tel. co. notifies before testing.
Company *M*		1½							No	Yes	No	$2.50 per cct. mile
		1½	No record	No record					No	No	No	One circuit is Ph. with GE ins. coil and WE 76A rept. coil.
		1							No	Yes	No	
		1¼							No	Yes	Yes	Exp. OW have high pot. GE ins. coils plus relay protectors.

S.c.—Supervisory Control. Tm.—Telemetering. Ind.—Indicating or Sup. Ind. * Serjedetours. † Relay protectors.

Table I (Continued)

Designation of Power Company	Type of Circuit or Identification**	Quest. 1		Quest. 2		Quest. 3		Quest. 4			Quest. 5	Quest. 6	Quest. 7	Quest. 8			Quest. 9	Quest. 10	Quest. 11	Quest. 12	
		Leased	Private	Cable	Open-Wire	Near	Distant	Metallic	Grd. Return	Phan.	Size of Cond.	Cond. Insulation	No. of Conds. in Cable	Untwisted	Twisted Pairs	Quadded	Type of Sheath	Length (Mi. One Way)	Res. of Loop Exc. Term. Eq. (Ohms)	Loaded	Nonloaded
Company *O*	S.c. & I.		x	x		x		x			#12	R	7	x			Pb	0.57			x
	S.c. & I.		x	x		x		x			#14	R	12	x			Pb	0.54			x
	S.c. & I.		x	x		x		x			#14	R	4	x			Braid	7.66			x
	HT tel. line		x		x	38′		x			#6							6.2			x
	HT tel. line		x		x	x		x			#9							28.0			x
	Tm	x		x			x	x										12.75	2,985		x
	Ind	x		x			x	x										3.75	650		x
	S.c. & I.	x		x			x	x										2.5	556		x
	Ind		x	x		x		x			#14	P	6	x			Pb	0.28			x
Company *P*			x		x	x		x			#8	4-kv. ins.		x				7½			
			x	x		x		x			#19	R+braid	4 used	x			Pb	1	9.9		x
			x		x	x		x			#8	4-kv ins.		x				3			
			x		x	x		x			#8	4-kv ins.		x				6½			
			x		x	x		x			#8	4-kv ins.		x				6			
			x		x	x		x			#8	4-kv ins.		x				1½			
			x		x	x		x			#8	4-kv ins.		x				6			
			x		x	x		x			#8	4-kv ins.		x				½			
Company *Q*	S.c.		x	x			x	x			#19	Paper	52		x		Pb	2.7	227		x
	S.c.		x	x			x	x			#19	Paper	152		x		Pb	2.2	185		x
	S.c.		x	x			x	x			#22	Paper	102		x		Pb	2	330		x
	S.c.		x	x			x	x			#19	Paper	52		x		Pb	3.1	267		x
	S.c.	x			x		x	x										1	48		x
	S.c.	x	x	x			x	x										2	330		x
	S.c.		x		x	x		x			#8 Cu. Cl.							1.5	37		x
	S.c.	x		x			x	x										7	915		x
Company *R*	S.c.		x	x		x		x			#12	4/64″	10				Pb	3.27	66.5		
Company *S*	Tm	x		2½	½	½–40′	2½	x										3			x
	S.c.	x		x			x	x										1.75	287		x
Company *T*	S.c.	x						x													x
	S.c.		x	x		x		x			#22	Single Paper	202		x		Pb	0.75			x
	S.c. & tm		x	x		x		x			#22	Single Paper	82		x		Pb	1.9			x
	S.c.	x		x			x	x										1.5			x
	S.c. & tm		x	x		x		x			#22	Single Paper	82		x		Pb	1.35			x
	S.c. & tm		x	x		x		x			#22	Single Paper	52		x		Pb	1.85			x
	Ind	x		¾	¼	¼	¾	x										1.0			x
	Ind		x	x			x	x			#19	Double Paper	202		x		Pb	1.46	120		x
	S.c. & tm		x	x			x	x			#19	Double Paper	102		x		Pb	3.05	264		x
	Ind		x	x		x		x			#22	Single Paper	152		x		Pb	2.11	360		x
	S.c. & tm		x	x			x	x			#19	Double Paper	202		x		Pb	0.33	27		x
	S.c.	x		½	1½	1½	½	x										1.75			x
Company *U*	S.c. & tm		x	x		x		x			#22	Paper 1,000–3,500	52	x			Pb	3	570		x
Company *V*	Tm		x		x	x				Ph-g		12 kv						77			x
	Tm		x		x		x			Ph-g		12 kv						7			x
Company *W*	S.c.	x		x			x	x										11.4	1,554	x	
	S.c.	x		x			x	x										3.26	511	x	
	S.c	x		x			x		x									8.95	268	x	
	Tm	x		x			x		x									11.4	1,153	x	
	S.c.	x		x			x	x										5.75	828	x	
	Tm	x		x			x		x									20.5	1,435	x	
	Tm	x		x			x		x									20.6	1,514	x	
	Tm	x		x			x		x									29.3	1,485	x	
	Tm	x		x			x		x									1.8	457	x	
	Tm	x		x			x		x									1.7	441	x	
	Tm	x		x			x	x										1.8	457	x	
	Tm	x		x			x		x									1.8	457	x	
	S.c.	x		x			x	x										5.4	1,010	x	
	Tm	x		x	x		x			Ph-g 5.2 mi.								37.5	1,554	x	
Company *X*	S.c.		x		x	x		x			#17	Rubber	2			x	Fabric	2.1	337		x
	Ind		x	x		x			x		#12	Rubber	3			x	Pb	1.6	21		x
	Ind		x	x		x		x			12/8	Rubber	3			x	Pb	1.9	29		x
	Ind	x		x		x		x				Paper					Pb		412		x
	Ind	x		x			x	x				Paper					Pb		919		x
	Ind		x	x		x		x			#12	Rubber	42			x	Pb	0.76	6.5		x
	Tm		x	x		x		x			#8	Rubber	7			x	Pb	3.5	26		x
	Ind		x		x	x		x			#17	Rubber	2			x	Fabric	0.3	47		x
Company *Y*			x	x		x		x			#16	Paper	48/12			x	Pb	21.8			x
			x	x		x		x			#16	Paper	48/12			x	Pb	21.8			x
			x	x		x		x			#16	Paper	48/12			x	Pb	21.8			x
			x	x		x		x			#16	Paper	48/12			x	Pb	21.8			x
			x	x		x		x			#16	Paper	48/12			x	Pb	21.8			x
Company *Z*	S.c.		x	x		x		x			#19	Paper	120			x	Pb	0.9	78.4		x
	S.c.		x	x		x		x			#16	Paper	50			x	Pb	1.2	35		x
	S.c.		x	x		x		x			#16	Paper	50			x	Pb				x
	S.c.		x	x		x		x			#16	Paper	50			x	Pb	0.85	36		x
	S.c		x	x		x		x			#16	Paper	70			x	Pb	0.6	25		x
	S.c.		x	x		x		x			#16	Paper	50			x	Pb	2.95	125		x
	S.c.		x	x		x		x			#16	Paper	50			x	Pb	0.7	30		x
	S.c.		x	x		x		x			#16	Paper	200			x	Pb	1.0	41		x
	S.c.		x	x		x		x			#16	Paper	20			x	Pb	3.2	137		x

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Table I (Continued)

Designation of Power Company	Type of Circuit or Identification**	Quest. 13 Terminal Protection								Quest. 14 Breakdown		Quest. 15 Potential Grad. Prot.				Quest. 16	Quest. 17 Treatment in Tel. Exchanges or Other Remote Pts.								Quest 18 Breakdown of Item 17	Quest. 19
		Carbon Blocks	Air Gaps	Thyrite	Protective Tubes	Vacuum Gaps	Fuses	Heat Coils	Relay Protect.	60 Cycles	Impulse	Neutralizing Trans.	Line Coup'g Trans.	Ins. Relay Equipment	Drainage Equipment	Ins. Level of Pilot Wire to Sta. Grd.	Carbon Blocks	Air Gaps	Thyrite	Protective Tubes	Vacuum Gaps	Fuses	Heat Coils	Relay Protect.	60 Cycles Impulse	Special Marking
Company O	S.c. & I.																									
	S.c. & I.																									
	S.c. & I.					x	x																			
	HT tel. line	x				x	x						x		x											
	HT tel. line	x				x	x						x													
	Tm.						x										x						x			Yes
	Ind.						x										x						x			Yes
	S.c. & I.						x										x						x			Yes
	Ind.																									
Company P					x	Sj*	x									16										
							x									65										
			x		x	Sj*	x									115										
					x	Sj*	x									50										
			x		x	Sj*	x									115										
			x			Sj*	x							x		65										Yes
			x			Sj*	x							x		65										Yes
					x	Sj*	x									50										
Company Q	S.c.	x	At disp. only					x		350				x		500 v										
	S.c.	x	At disp. only					x		350				x		500 v										
	S.c.	x	At disp. only					x		350				x		500 v										
	S.c.	x	At disp. only					x		350				x		500 v										
	S.c.					x				450				x		500 v										
	S.c.	x						x		350				x		500 v	x						x		350	
	S.c.					x				450				x		500 v										
	S.c.	x								350		x				500 v	x						x			
Company R	S.c.				x		x			300–500																
Company S	Tm.	x		x						500	on ord.						x					x			500	Yes
	S.c.	x															x					x			500	Yes
Company T	S.c.	x															x					x			500	Yes
	S.c.	x					x																		500	
	S.c. & tm.	x					x			500															500	
	S.c.	x					x										x					x			500	Yes
	S.c. & tm.	x					x																			
	S.c. & tm.						x																			
	Ind.	x					x										x					x			500	Yes
	Ind.	x					x			500																
	S.c. & tm.	x					x																			
	Ind.	x					x																			
	S.c. & tm.						x																			
	S.c.	x					x										x					x			500	Yes
Company U	S.c. & tm.				x					350						3,500										
Company V	Tm.		x			x	x						x		x	15kv.										
	Tm.		x			x	x						x		x	15kv.										
Company W	S.c.				x		x			500		Not used					x								350	Yes
	S.c.			x			x			150		Not used					x								350	Yes
	S.c.						x					Not used					x								350	Yes
	Tm.				x		x			90		Not used					x								350	Yes
	S.c.			x			x			150		Not used					x								350	Yes
	Tm.				x					90		Not used					x								350	Yes
	Tm.				x					90		Not used					x								350	Yes
	Tm.				x					90		Not used					x								350	Yes
	Tm.				x					90		Not used					x								350	Yes
	Tm.				x					90		Not used					x								350	Yes
	Tm.				x					90		Not used					x								350	Yes
	Tm.				x					90		Not used					x								350	Yes
	S.c.				x					150		Not used					x								350	Yes
	Tm.				x					90		Not used					x								350	Yes
Company X	S.c.				x		x	HiVF		400					x											Unnec.
	Ind.						x																			Unnec.
	Ind.																									Unnec.
	Ind.	x					x															x				Yes
	Ind.	x					x															x				Yes
	Ind.						x																			Unnec.
	Tm.																									Unnec.
	Ind.				x		x	HiVF		400					x											Unnec.
Company Y		x			x		x			600					x		x			x		x			600	No
		x			x		x			600					x		x			x		x			600	No
		x			x		x			600					x		x			x		x			600	No
		x			x		x			600					x		x			x		x			600	No
		x			x		x			600					x		x			x		x			600	No
Company Z	S.c.	x								350																
	S.c.	x								350																
	S.c.	x								350																
	S.c.				x					350																
	S.c.	x								350																
	S.c.	x								350																
	S.c.	x								350																
	S.c.	x								350																
	S.c.	x								350																

S.c.—Supervisory Control. Tm.—Telemetering. Ind.—Indicating or Sup. Ind. * Serjedetours. † Relay protectors.

Table I (Continued)

		Quest. 20	Quest. 21	Quest. 22	Quest. 23 No. of Outages Due to:				Quest. 24	Quest. 25	Quest. 26	
Designation of Power Company	Type of Circuit or Identification**	Years of Service	No. of Outages During Service	Total Outage Time (Hrs.)	Short Circuits	Open Circuits	Circuit Rearrangements	Terminal Protect.	Pilot Wire Automatically Super'd?	Pilot Wire Used for Other?	Troubles Due to Induction or Surges?	Miscellaneous Information as Covered in Questionnaire Under "Additional Information." In General These Data Were Not Suitable For Summarizing
Company *N*		13							Yes.-Mo.	No	Yes	Difficulty from induced voltages. Only grounded thru detectors. Meggered monthly at 500 volts.
		13	Some early troubles due to Matthews test caps. Now have some difficulty from transverse voltages.						Yes.-Mo.	No	Yes	
		13							Yes.-Mo.	No	Yes	
		10							Yes.-Mo.	No	Yes	
		13							Yes.-Mo.	No	Yes	
		4							Yes.-Mo.	No	Yes	
		1½							Yes.-Mo.	No	Yes	
		6							Yes.-Mo.	No	Yes	
Company *O*	S.c. & I.	13							No	No	No	
	S.c. & I.	15							No	No	No	
	S.c. & I.	20							No	Yes	No	
	HT tel. line	11							No	No	Yes	
	HT tel. line	21							No	No	Yes	
	Tm.	3							No	No	No	
	Ind.	13							No		No	
	S.c. & I.	12							No	Yes	No	
	Ind.	15							No	No	No	
Company *P*		8	50	75	5	3		42	Yes	No	No	Terminal equipment ungrded. except through station battery. Auto. detection of accidental grds. and incipient faults.
		13	70	150	Failure of sup. eq.				Yes	No	No	
		7	5	3	1	1		3	Yes	No	No	
		6	20	96		15			Yes	Yes TP	Only minor	
		7	4	8		2		2	Yes	No	No	New inst. prefer cable prs.
		7	30	60	12	13			Yes	Yes TM	No	Some failures due to fog.
		7	35	70	3	2			Yes	Yes TM	No	
		6	10	48	Sup. equip. 10				Yes	Yes TP	Only minor	
Company *Q*	S.c.	14							No	No	No	Equipment is grounded at sta. when grd. is <0.5 w at dispatcher's end.
	S.c.	14							No	No	No	
	S.c.	14							No	No	No	Sta. end ungrounded.
	S.c.	14							No	No	No	Would want auto. super. on new inst
	S.c.	4	Occa. relay coil burnout fuses						No	No	Yes	
	S.c.	10							No	No	No	
	S.c.	15	Occa. relay coil burnouts						No	No	Yes	
	S.c.	1							No	No	No	
Company *R*	S.c.	1½							No		No	Grounded only through prot. tubes.
Company *S*	Tm.	7½	3	7	1			2	No	No	No	$.875 per ½ mile.
	S.c.	1	0						No	No	No	
Company *T*	S.c.	0							No	No		Most of terminal equipment grounded to remote grounds.
	S.c.	14	No record								No	
	S.c. & tm.	14	No record								No	
	S.c.	13	1 reg. & 1 emergency pair						Yes		No	
	S.c. & tm.	15	Numerous out. due to grd. prot.-corrected								No	
	S.c. & tm.	15	Numerous out. due to grd. prot.-corrected								No	
	Ind.	3	1	52	1				Yes		No	
	Ind.	14	3	32	3 (vandalism)						No	
	S.c. & tm.	13	4	89	4 (2 sh. injuries—2 vandalism)				Yes		No	
	Ind.	14	Many trbls. & large amt. of lost time								No	Poor cable.
	S.c. & tm.	13	1	127			Flood				No	
	S.c.	10	5					5	No		No	
Company *U*	S.c. & tm.	New	No oper. exp.									Taps from cable 500/1400.
Company *V*	Tm.	10	No record						No	Tel.	Yes	Would prefer ccts. in own cable but exp.
	Tm.	2+	No record						No	Tel.	Yes	Possibly auto. super. $1.25 per wire mile per month.
Company *W*	S.c.	6.1	0						No	No	No	Grounded to sta. grd.
	S.c.	4.3	0						No	No	No	No exp. to show grd. prot. is adequate.
	S.c.	0.8	0						Yes	No	No	Leased because maintenance is superior and cheaper. Channel can easily be duplicated in case of prolonged outages.
	Tm.	3.6	6	5.2		5			No	No	No	
	S.c.	2.8	1	1.7		1			No	No	No	
	Tm.	8.3	4	5		4			No	No	No	
	Tm.	3.5	9	7	1	7			No	No	No	Troubles on leased ccts. not coincident with trans. line (part from induction).
	Tm.	3.6	9	5.6		9			No	No	No	
	Tm.	3.5	0						No	No	No	Prefer auto. super. Thyrite or UG's.
	Tm.	3.5	0						No	No	No	Supervision by means of relay which drops out in reduced current.
	Tm.	8.3	0						No	No	No	
	Tm.	8.4	0						No	No	No	Grds. detected by sensitrol relay conn. between center tap of pilot wire relay battery and grd.
	S.c.	3	3	14.8		3			Yes	No	No	
	Tm.	0.2	1	10.5		1			No	No	No	
Company *X*	S.c.	1½	11	110	7	2		2	No	No		$4 per mile per pr.
	Ind.	16	No record						Yes	No	Yes	Power sta. grd.
	Ind.	16	No record						Yes	No	No	Many tm. ccts. continuously energized and troubles indicated by instruments.
	Ind.	12	No record						No	No	No	
	Ind.	12	No record						No	No	No	Early troubles from surges relieved by resistor.
	Ind.	14	No record						Yes	No	No	
	Tm.	22	No record						Yes	No	No	
	Ind.	1½	0						No	No	No	
Company *Y*		15	No record available									
		15	No record available									
		15	No record available									
		15	No record available									
		15	No record available									
Company *Z*	S.c.	8							No	No		
	S.c.	6	2	13	1		1		No	No		
	S.c.	10	9	34	4		5		No	No		
	S.c.	9							No	No		
	S.c.	12	1	3			1		No	No		
	S.c.	11	1	4½	1				No	No		
	S.c.	15	2	3	1		1		No	No	Yes	
	S.c.	17	3	21	1		3		No	No		
	S.c.	17	6	25½	2		4		No	N	Yes	

S.c.—Supervisory Control. Tm.—Telemetering. Ind.—Indicating or Sup. Ind. * Serjedetours. † Relay protectors.

Automatic supervision....................4 companies preferred
1 company did not prefer

3. Circuits in cable.....5 companies preferred
In underground cable..............1 company preferred

4. Circuits along separate route from power..................1 company preferred

5. (*a*) Leased circuits....3* companies preferred
(*b*) Power-line carrier..............1** company preferred
(*c*) Privately owned cable............ 1 company preferred

(*e*). Is terminal equipment grounded to power-station ground, external ground, or ungrounded? (question 13)

1. Number of companies answering............10
2. Number of companies using station ground.. 7
3. Number of companies using remote ground.. 2
4. Number of companies leaving ungrounded... 1

Table II

	Average Years In Service Per Circuit	Average Total Number of Outages Per Circuit	Average Total Outage Time Per Circuit (Hours)	Average Length of Circuit (Miles)	Average Duration Of outage Per Circuit (Hours)	Average Number outages Per Circuit Mile-Year	Average Outage Time Per Circuit Mile-Year (Hours)
Basis A							
Privately owned	10.3 (71)*	4.45 (60)	18.8 (60)	6.2 (105)	4.22 (60)	0.06 (60)	0.25 (60)
Leased	4.9 (70)*	4.4 (59)	16.7 (59)	6 4 (69)	3.80 (59)	0.12 (59)	0.45 (59)
Total	7.6 (141)*	4.43 (119)	17.75 (119)	6.3 (174)	4.01 (119)	0.09 (119)	0.35 (119)
Basis B							
Privately owned	7.9 (27)*	9.8	40.4	3.7	4.13	0.33	1.37
Leased	5.1 (38)*	5.0	22.7	7.0	4.53	0.14	0.64
Total	6.3 (65)*	7.0	30.1	5.6	4.30	0.23	0.95

* Figures in parentheses refer to number of circuits involved in average. In basis *B* the figures (27), (38) and (65) apply to the corresponding entries in all columns.

Appendix. Questions Submitted to Operating Organizations for Information on Pilot-Wire Conductors

Circuit Details

1. *L*—Leased; privately owned.

2. *C*—Cable; open-wire.

3. *D*—Located at a distance from power lines.
A—Located adjacent to power lines.

4. *M*—Metallic; *G*—Ground return; *P*—Phantom.

†5. Size of conductor.

†6. Conductor insulation.

†7. Number of conductors in cable.

†8. Conductor arrangement:

U—Untwisted.
T—Twisted pairs.
Q—Quadded.

†9. Type of sheath.

10. Length (miles one way).

11. Resistance (ohms—loop exclusive of terminal devices).

12. *L*—Loaded; *U*—Unloaded.

Terminal Protection

13. *C*—Carbon blocks; *AG*—Air gaps; *T*—Thyrite; *PT*—Protective tubes; *VG*—Vacuum gaps; *F*—Fuses; *H*—Heat coils; *R*—Relay protectors.

14. Breakdown values of item 13.

(*a*) 60 cycle.
(*b*) Impulse.

Potential Gradient Protection

15. *N*—Neutralizing transformers; *C*—Line-coupling transformers; *I*—Insulated relay equipment; *D*—Drainage equipment.

16. Insulation level of pilot wire to station ground at item 15.

Treatment in Telephone Exchanges or at Other Remote Points

17. Protection as in item 13.

18. Breakdown values of item 17:

(*a*) 60 cycle.
(*b*) Impulse.

19. Special marking to prevent routine testing.

Performance

20. Years of service.

21. Pilot-wire outages (number during item 20).

22. Total outage time (hours).

23. Number of outages caused by:

S—Short circuits.
O—Open circuits.
C—Circuit rearrangements.
P—Terminal protection.

24. Is pilot-wire circuit automatically supervised?

25. Is pilot-wire circuit used for other purposes?

26. Have troubles been experienced because of induction effects or surges on the pilot wires?

Additional Information

1. If the circuit is leased, what is the basic rental rate?

3. If the circuit is located on the same right of way with a power-line phase, give details as to the construction of the power circuit. What provisions are made to prevent troubles from induced voltages, both between the pilot conductor and between the pilot conductors and ground?

4. Please give details if a phantom circuit is used.

12. Does capacity and loading of the pilot circuit affect the performance, and what compensation methods if any are employed?

13. Is the terminal equipment grounded to the power-station ground, external ground, or ungrounded?

13 to 16. Please indicate by a sketch the arrangement used indicating the position of ground connections. What evidence either from experience or special tests do you have to establish that the protection afforded is adequate?

24. Please describe the monitoring system employed and values of monitoring current, and indicate whether or not grounds on the pilot wires are detected. What minimum insulation resistance is permitted between conductors before circuit is considered bad? What value is detected? Have incipient faults thus been discovered?

25. Please describe other joint uses of the pilot wires.

26. Please discuss troubles experienced caused by induction or surges and means taken to prevent them.

27. For new installation, what type of pilot channel would you prefer? Why?

28. For new installations would you want to use automatic supervision?

29. For new installations what form of terminal or intermediate protection (items 13 and 17) would you prefer?

Reference

1. PILOT-WIRE CIRCUITS FOR PROTECTIVE RELAYING—EXPERIENCE AND PRACTICE, AIEE relay subcommittee. AIEE TRANSACTIONS, volume 62, 1943, May section, pages 210–14.

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* 1 for the shorter distances.

** For the longer distances.

† Privately owned circuits only.

Theory of Rectifier—D-C Motor Drive

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THE use of rectifiers for furnishing power to d-c motors is becoming more common, particularly for variable-voltage drives. The theory applying to drives such as those incorporating the Ward Leonard system is quite inadequate for use with a rectifier–d-c motor drive. This paper is an introduction to the theory involved when operating a d-c shunt-wound motor from a rectifier. It is hoped that further thought and discussion of this new and vital subject will be stimulated.

In the course of the development work on the system described in a paper by K. P. Puchlowski entitled "Electronic Control of D-C Motors,"[1] it became apparent that purely empirical data on performance were unsatisfactory for thorough analysis of the system. This paper describes the theoretical approach to the obtaining of fundamental characteristics of a complete drive consisting of a single-phase full-wave electronic rectifier and a d-c motor. The rectifier output is variable by grid control of the thyratrons combined with a control circuit for the rectifier. The rectifier for the larger motors is usually multiphase. The control circuit regulates to any manually preset motor speed, compensates for armature *IR* drop, and incorporates current-limit acceleration.

We are concerned here with the broad considerations of a d-c shunt-wound motor operated from a grid-controlled electronic rectifier rather than the details of the speed-control circuits for the drive. Only a single-phase full-wave rectifier is considered. This type of rectifier is normally used on motors of low horsepower rating, usually not over three horsepower.

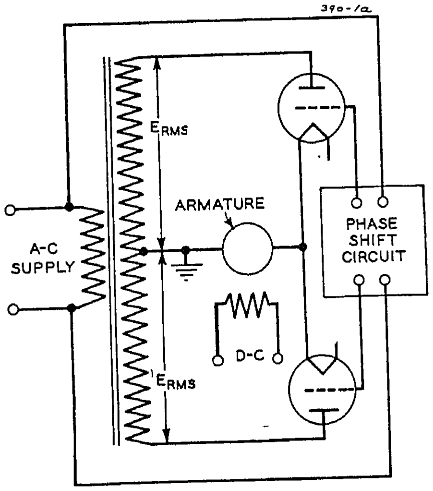
General Considerations

The simplified circuit of a typical single-phase full-wave system is shown in Figure 1A. In this particular circuit, the rectifier is composed of two thyratron tubes with their grids arranged for phase-shift control to permit the tubes to start conducting (firing) at any chosen phase angle, with respect to the applied a-c voltage, during each half-cycle.

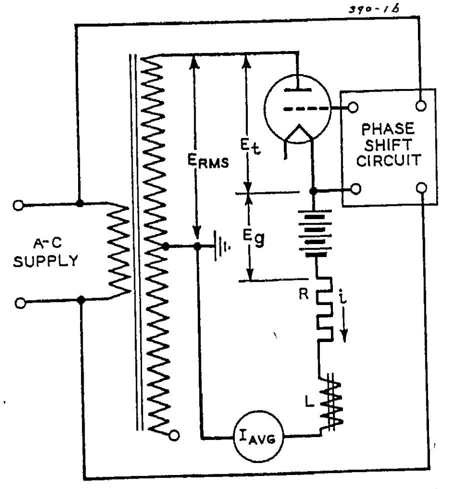
The simplified circuit in Figure 1A has been converted into an equivalent circuit in Figure 1B to show more clearly the basic considerations and terminology. Several very important assumptions have been made in the conversion from Figure 1A to Figure 1B, and these are used throughout the paper.

Assumptions

1. The machine is shunt wound with no compounding.
2. The field is continuously energized and saturated.



A. The elementary diagram of a single-phase full-wave rectifier—d-c motor drive



B. A circuit equivalent to one half of the full-wave rectifier shown in A

Figure 1

3. The armature inductance is constant.
4. The armature and circuit inductances have been lumped in the inductance *L*.
5. The resistances of the armature and the external circuit have been lumped in the resistance *R* and are constant.
6. The counter electromotive force, E_g, is directly proportional to motor speed and is represented as a battery with no internal resistance.
7. The transformer inductance and resistance are negligible.
8. The arc drop of the tube is shown as E_t and is constant.
9. The tube characteristics are such that conduction can begin as soon as the instantaneous value of the transformer voltage is equal to or greater than E_g+E_t.
10. The effect of armature reaction is negligible.
11. The individual half-cycles of current are discreet pulses which do not join each other. Hence the equivalent circuit shows only one half of the full-wave rectifier.
12. All calculations apply to one half-cycle in view of (11) above.

Some of the assumptions are exact while some are close approximations. All are sufficiently accurate that the resulting data serve as a very good guide for analysis of test results and further elaboration of the theory.

For convenience, the term E_m is used in the appendix to denote $\sqrt{2}E_{\text{rms}}$. When the formulas are transferred from the appendix to this text, the form $\sqrt{2}E_{\text{rms}}$ is used.

Firing (Starting) and Stopping Angles of Current Pulses

When a d-c motor is being driven from a single-phase full-wave rectifier as shown in Figure 1A, the available input voltage E_{rms} has instantaneous values *e* as shown in Figure 2A. The rectifying effect is shown by subsequent half-cycles of voltage *e* being all positive with respect to the zero line. The counter electromotive force E_g is shown as less than the peak voltage $e_{\max}$ of the a-c wave. This must always be so, since otherwise no armature current would ever flow. During any of the time that *e* is more than E_g+E_t, current flow can be started by proper control of the phase angle of the grid voltage.

Since it has been assumed that the individual half-cycles are discreet pulses which do not join each other, we need consider only a single half-cycle of events, at least within the limits where the assumption holds. The limits will be discussed later in more detail. In Figure 2, X_f is the "firing" angle at which the grid

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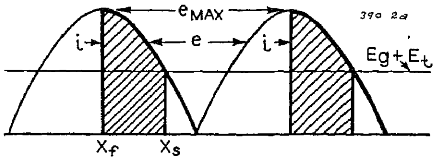
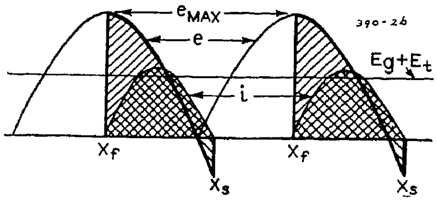


A. The wave form of current and voltage in a full-wave rectifier operating the d-c motor which is assumed to have no inductance. The effect of rectification is shown by locating both halves of the a-c cycle of voltage and current above the zero line where the zero line is at the potential of the transformer midpoint



B. Same as A except with inductance present

Figure 2

phasing permits the thyratron tube to start conduction. The thyratron may be considered as a rectifying single-pole switch which closes at the "firing" angle X_f. If the armature inductance L in Figure 1B were zero, the current would flow until, in Figure 2A, e becomes less than E_g+E_t at angle X_s, which is called the "stopping angle." The current must stop flowing at the angle X_s, because the opposing voltages are equal, and the rectifying property of the thyratron does not permit reversal of the current. The voltage e may be considered as applied to the armature during the interval X_f to X_s, and thus the hatched portion of Figure 2A represents the voltage applied to the armature by the rectifier. This wave shape is not directly measurable in an actual system, because it cannot be conveniently disassociated from the counter electromotive force E_g. It should not therefore be thought of as the measureable armature voltage which is a more involved function. If the inductance were zero, the current pulse in Figure 2A would have the same shape as the voltage wave from X_f to X_s.

The ideal case of zero armature inductance is quite far from the actual one, and Figure 2B shows the modified relations with inductance present. The general effect of the inductance is to reduce the peak of the current pulse and broaden its base by causing the current to continue flowing beyond the point where e equals E_g+E_t. The applied voltage during the interval X_f to X_s is again shown single-hatched in Figure 2B, but the current now does not follow the shape of the applied voltage and is represented by the double-hatched curve i. The general shape of the current wave i resembles a sine wave but must not be mistaken for one. The voltage e from X_f to X_s is somewhat peculiar when inductance is present, since the current may flow until the voltage is either positive or negative at X_s with respect to ground.

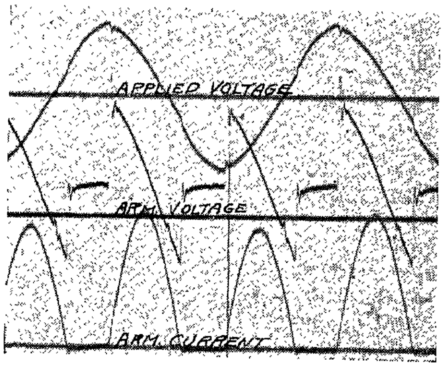
The oscillograms in Figure 3 show clearly the relations actually obtained between a-c voltage, current, and armature voltage at two different conditions of load and speed.

The fundamental relationship on which most of this paper is based is that of the "stopping angle" X_s as a function of the "firing angle" X_f with other variables as parameters and is shown in Figure 4. The mathematical equation from which the curves were drawn is derived in the appendix, part B and is as follows:

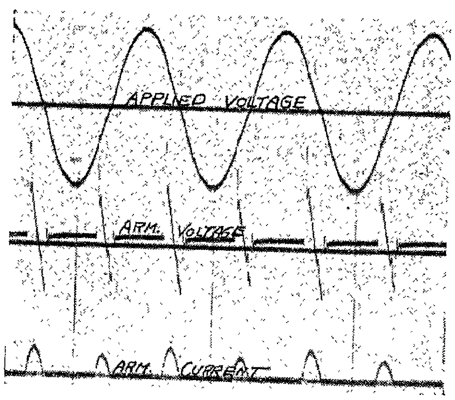
$$\cos\theta\ \sin(X_s-\theta)-a+[a-\cos\theta\ \sin(X_f-\theta)]\epsilon^{-\frac{X_s-X_f}{\tan\theta}} \quad (27)$$

or another form is

$$[a-\cos\theta\sin(X_s-\theta)]\epsilon^{\frac{X_s}{\tan\theta}} = [a-\cos\theta\ \sin(X_f-\theta)]\epsilon^{\frac{X_f}{\tan\theta}} \quad (28)$$



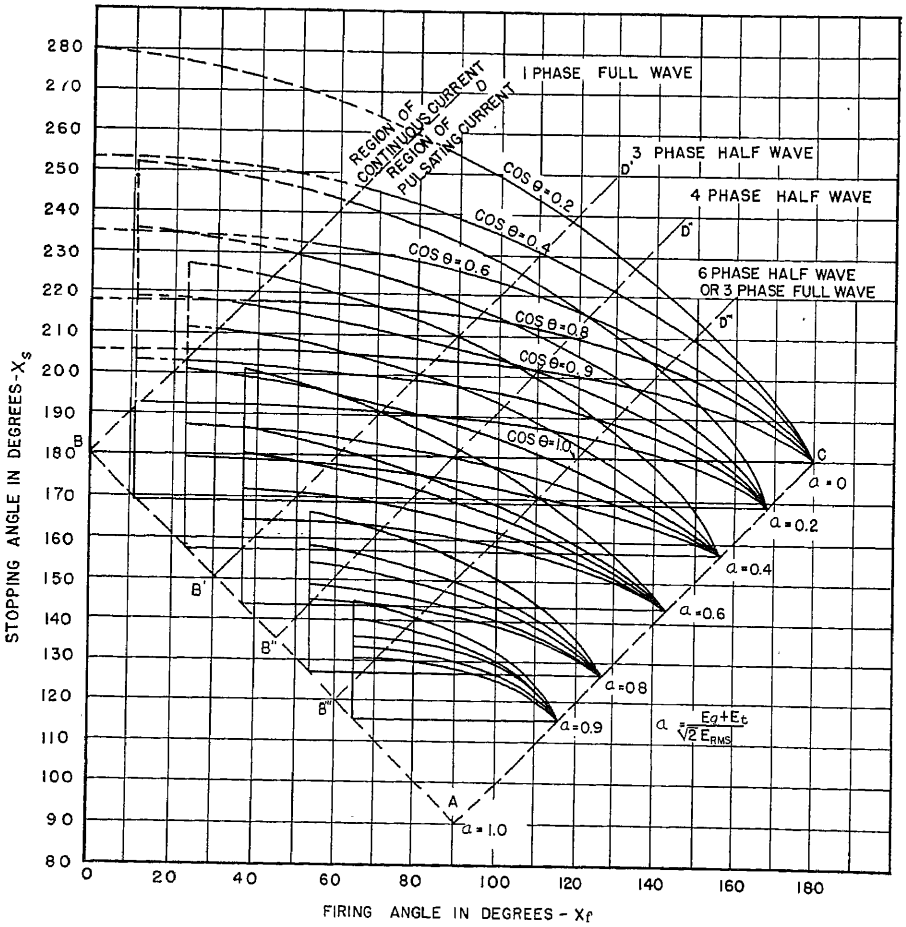
A. Full rated speed and torque



B. No load and 16 per cent of rated speed

Figure 3. Oscillograms of current and voltage in an electronic d-c motor drive with constant field

Figure 4. Graph showing "stopping angle" X_s of current as a function of "firing angle" X_f with cos θ and "speed factor" a as parameters



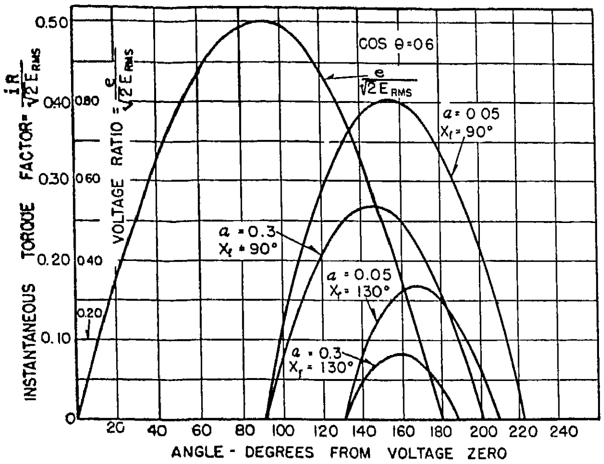


Figure 5. Calculated wave shap of current pulse for two "firing angles" X_f and two "speed factors" *a*

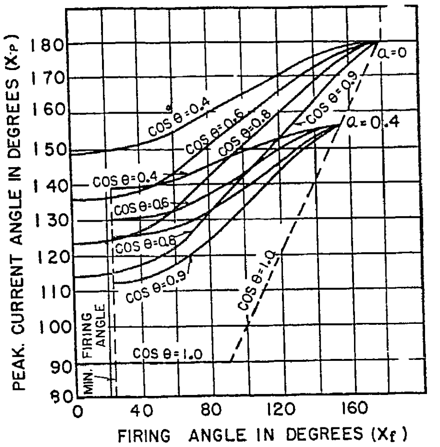


Figure 6. Curves showing the angle X_p at which peak current occurs as a function of "firing angle" X_f for two "speed factors" and several values of cos θ

where

$a = \frac{E_g+E_t}{\sqrt{2}E_{rms}}$ called "speed factor" in this paper

$\theta = \text{arc}\tan\frac{\omega L}{R}$ = power-factor angle of the motor armature circuit at the a-c line frequency

The "speed factor" *a* is a convenient means of expressing, as a ratio, the a-c voltage E_{rms} and the counter electromotive force of the live load consisting of the voltage E_g and the tube arc drop E_t. The "speed factor" is so named, because E_g is assumed to be directly proportional to motor speed.

Several interesting points should be noted in regard to Figure 4. Obviously, the firing angle X_f in Figure 2 cannot be greater than the greatest angle at which *e* becomes equal to E_g+E_t. Since the limit is a function of E_g and consequently of *a*, there is a locus of maximum possible firing angle X_f. This locus is shown in Figure 4 as line *A–C* which is the beginning of all curves.

Also, it is obvious that the firing angle X_f cannot be less than that angle at which *e* first becomes greater than E_g+E_t. This limit is shown in Figure 4 by termination of the curves, which begin on *A–C*, at firing angles projected vertically from the line *A–B*. The same limit holds for all values of cos θ at a given value of *a*.

If $X_s-X_f>180$ degrees, the flow of current will no longer be in discreet half-cycle pulses but will be continuous. Equations 27 and 28 do not hold for conditions of continuous current. The limits within which the equations hold true are established in Figure 4 by the lines *B–D*, *B′–D′*, *B″–D″*, *B‴–D‴*.

Wave Shape of Current Pulse

The wave shape of the current pulse resembles that of a sine wave. It is, however, mathematically speaking, a combination of a sine wave, a constant negative term, and an exponential function. The equation for the current pulse is derived in the appendix, part A and is

$$\frac{iR}{\sqrt{2}E_{rms}} = \cos\theta\ \sin(x-\theta) - a + [a-\cos\theta\ \sin(X_f-\theta)]\epsilon^{-\frac{x-X_f}{\tan\theta}} \quad (26)$$

where x = any angle greater than X_f but less than X_s. A graph of this equation is shown in Figure 5 with firing angle X_f and speed factor *a* as parameters while cos θ is kept constant. The expression $iR\div\sqrt{2}E_{rms}$ serves to give a ratio which eliminates specific values of current and will be called "instantaneous torque factor." It is actually the instantaneous *IR* drop in per cent of peak a-c voltage. The parameters which have been chosen for illustration in Figure 5 are typical of those in a practical drive.

Peak Current and Its Angle of Occurrence

The angle X_p at which peak current flows is readily obtained by differentiating the equation 26 and setting it equal to zero as shown in the appendix, part C. This gives the equation

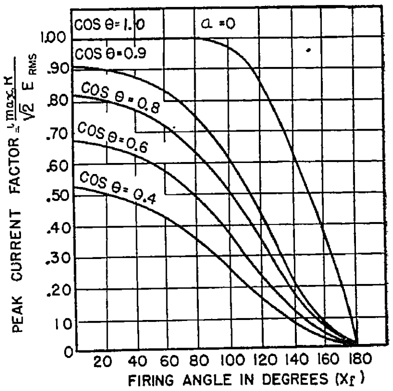
$$\cos(X_p-\theta)\epsilon^{\frac{X_p}{\tan\theta}} = \left[\frac{a}{\sin\theta} - \frac{\sin(X_f-\theta)}{\tan\theta}\right]\epsilon^{\frac{X_f}{\tan\theta}} \quad (49)$$

Data from equation 49 are plotted in Figure 6 which shows the peak-current angle X_p as a function of the firing angle X_f for parameters of cos θ and speed factor *a*.

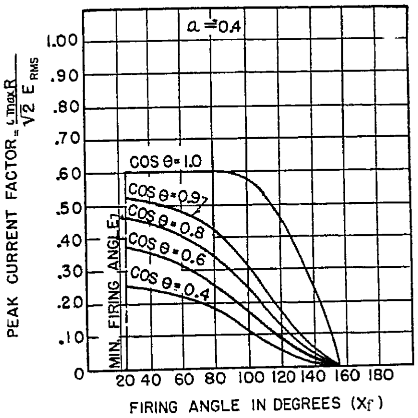
The peak value of the "instantaneous torque factor" $iR/\sqrt{2}E_{rms}$ can now be calculated by substituting the value of X_p calculated from equation 49 for x in equation 26. At the maximum value of current we may substitute i_{max} for i in the expression $iR/\sqrt{2}E_{rms}$ giving $i_{max}R\div\sqrt{2}E_{rms}$ which will be called "peak-current factor." A graph showing "peak-current factor" as a function of firing angle X_f is shown in Figure 7.

Armature Load–Speed Relations

The purpose of these calculations is to obtain performance characteristics in addition to the more fundamental data.



A. When "speed factor" = 0



B. When "speed factor" = 0.4

Figure 7. Curves showing the "peak-current factor" as a function of "firing angle" X_f for several values of cos θ

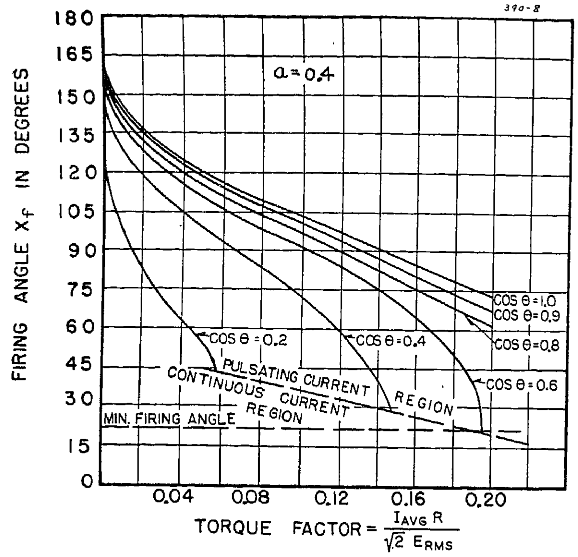


Figure 8. Curves showing the "firing angle" X_f required to maintain the "speed factor" $a=0.4$ for various values of "torque factor" and cos θ

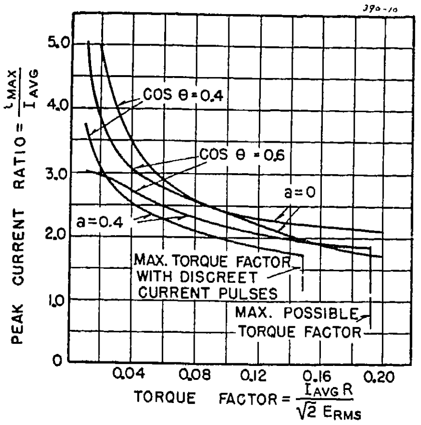


Figure 10. The ratio of the current at the peak of the current pulse to the average armature current shown as a function of "torque factor" for two values of cos θ and two of "speed factor" a

The necessary step in achieving this is to determine the torque–firing-angle relationship. This is done in the appendix, part D, and results in an equation for the average armature current I_{avg} as a function of the firing angle X_f. The equation for I_{avg} follows:

$$I_{\text{avg}}=\frac{\sqrt{2}E_{\text{rms}}}{\pi R}[\cos X_f-\cos X_s-a(X_s-X_f)] \tag{57}$$

It is convenient to express equation 57 as follows:

$$\frac{I_{\text{avg}}R}{\sqrt{2}E_{\text{rms}}}=\frac{1}{\pi}[\cos X_f-\cos X_s-a(X_s-X_f)] \tag{58}$$

The expression $I_{\text{avg}}R/\sqrt{2}E_{\text{rms}}$ will be called the "torque factor," inasmuch as its value is directly proportional to torque if the developed motor torque is assumed to be directly proportional to armature current. The formula, therefore, makes no allowance for friction and windage. The useful torque is obtained by subtracting that attributable to friction and windage from the calculated value. The torque factor is useful as a means of eliminating consideration of motor size and details.

Equation 58 is plotted in Figure 8 showing the firing angle as a function of the torque factor. Cos θ is used as a parameter, and the whole curve corresponds to a fixed value of speed factor a and is a constant-speed graph. This shows very clearly the effect of inductance or power-factor angle on the firing angle X_f required to maintain a speed factor $a=0.4$ for a varying torque factor.

The data are presented a little differently in Figure 9 where cos $\theta=0.4$ and the speed factor is a parameter. These are, of course, curves of most practical interest for a given motor. The curves in Figure 9 show the very considerable change of firing angle required to maintain constant speed as the torque factor is varied. Obviously, space does not permit showing complete curves for a full range of both cos θ and speed factor. Hence, only the two typical curves in Figures 8 and 9 have been shown.

Peak-Current Ratio

The term "peak-current ratio" as used here denotes the ratio $i_{\max}/I_{\text{avg}}$. This gives a conception of the peak current which the tubes must carry as related to the known I_{avg} required to deliver the desired torque. This ratio is obtained by dividing the "peak-current factor" $i_{\max}R/\sqrt{2}E_{\text{rms}}$ by the "torque factor" $I_{\text{avg}}R/\sqrt{2}E_{\text{rms}}$ from Figure 9. This gives the "peak-current ratio" $i_{\max}/I_{\text{avg}}$, which can be plotted as a function of the torque factor. A typical set of curves for the peak-current ratio is shown in Figure 10. It is seen that the electronic rectifier tube must therefore be selected with a peak-current rating of several times the maximum I_{avg} required by the motor during the starting inrush.

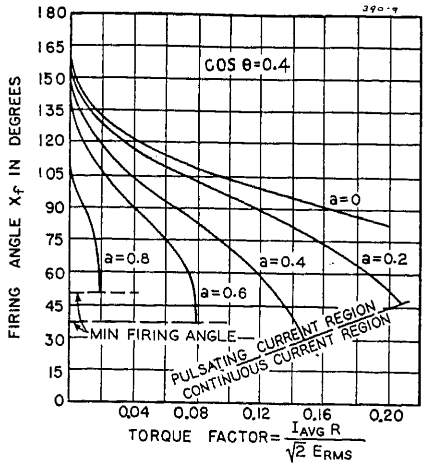
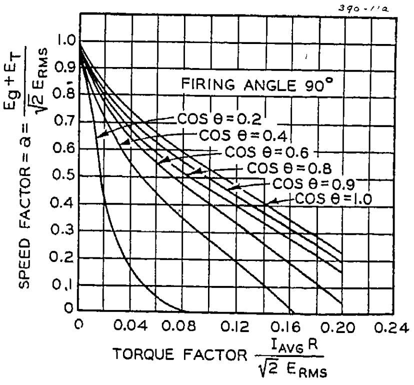


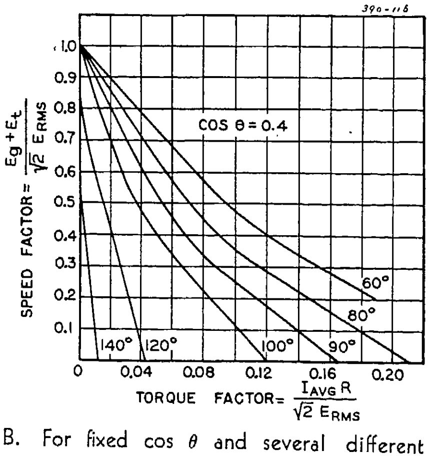
Figure 9. The "firing angle" required to maintain various "speed factors" at different values of "torque factor"; cos θ is held constant

Speed-Torque Curves

The data contained in Figures 8 and 9 are in such form that we can directly replot them to give the speed factor a as a function of the torque factor. The speed-torque curves in Figure 11A are replotted from Figure 8 and show the speed regu-



A. For a fixed "firing angle" and several values of cos θ



B. For fixed cos θ and several different "firing angles"

Figure 11. Speed–torque characteristics

lation for a typical constant firing angle of 90 degrees for various values of cos θ. The curves in Figure 11B are replotted from Figure 9 and show the speed regulation at cos $\theta=0.4$ for various fixed firing angles.

Conclusions

1. The calculated wave shapes are verified by the oscillograms.

2. The speed-torque curves of a single-phase full-wave-rectifier–d-c motor drive without automatic speed regulation are such as to be useful only for specialized applications.

3. Most applications will require that the drive incorporate automatic speed regulation.

4. Additional work to correlate mathematical and test results would be desirable. This work should cover various sizes, speeds, and types of motors.

5. Further refinement of the theory and extension to multiphase rectifiers would be helpful.

6. It is hoped that this paper will serve its stated purpose of stimulating discussion of this new subject.

Appendix. Mathematical Analysis of the D-C Motor–Rectifier System

Part A. Equation of the Current Pulse

The equivalent circuit of a single-phase half-wave rectifier system with armature of a d-c motor as a load is represented in Figure 13.

It is assumed that:

1. R = resistance of the armature circuit and is constant.
2. L = inductance of the armature circuit and is constant.
3. E_g = counter electromotive force induced in armature winding of the motor and is directly proportional to the speed.
4. E_t = arc drop of the mercury-vapor or gaseous-discharge rectifier and is constant.

Voltages E_g and E_t may be regarded as constant components of the voltage applied to the system and are represented in Figure 13 by a battery without any internal resistance and an electromotive force $=E_g+E_t$.

The method is based upon the mathematical solution of a general circuit shown in Figure 12 and subsequent restriction of the domain of definition of the independent variable (time) and of its function (current) in accordance with the physical property of unidirectional conduction (rectification). These limitations will be discussed later and will become apparent from Figure 14 where the applied a-c voltage is represented as function of electrical angle (or time).

Referring to Figure 14, it will be seen that the sinusoidal voltage applied to the system is the following function of the electrical angle:

$$e=E_m \sin x \tag{1}$$

Denoting the firing angle of the rectifier (angle of ignition) by X_f we may write

$$x=X_f+\omega t \tag{2}$$

where

$\omega=2\pi f$ = angular frequency of the applied voltage

t = time counted from the instant $\frac{X_f}{\omega}$ (for $x=X_f$, $t=0$)

and the a-c voltage applied to the system can be expressed as

$$e=E_m \sin(X_f+\omega t) \tag{3}$$

and

$$(RB-\omega LA)\sin(X_f+\omega t)+(RA+\omega LB)\times \cos(X_f+\omega t)+RC\equiv E_m\sin(X_f+\omega t)-(E_g+E_t) \tag{7}$$

In order to satisfy the identity 7 the coefficients A, B, and C must satisfy the set of the following equations:

$$\left.\begin{aligned} RB-\omega LA&=E_m\\ RA+\omega LB&=0\\ RC&=-(E_g+E_t)\end{aligned}\right\} \tag{8}$$

and from equations 8 we get

$$A=-\frac{\omega LE_m}{R^2+\omega^2L^2} \tag{9}$$

$$B=\frac{RE_m}{R^2+\omega^2L^2} \tag{10}$$

$$C=-\frac{E_g+E_t}{R} \tag{11}$$

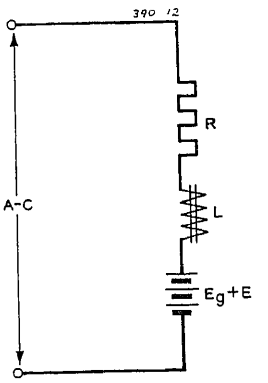


Figure 12. An elemental circuit with resistance, inductance, and a d-c voltage connected in series and supplied from an a-c line

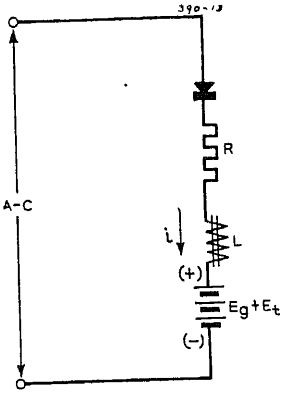


Figure 13. An elemental half-wave rectifier circuit with resistance, inductance, and a counter electromotive force in the load

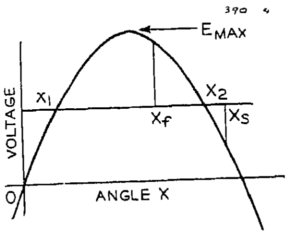


Figure 14. A sine wave of voltage with counter electromotive force

The general equation of the circuit (Figure 12) can now be written in the following form:

$$L\frac{di}{dt}+Ri+E_g+E_t=E_m\sin(X_f+\omega t) \tag{4}$$

or

$$L\frac{di}{dt}+Ri=E_m\sin(X_f+\omega t)-(E_g+E_t) \tag{5}$$

where $i=f(t)$ = current flowing in the circuit.

Equation 5 will be solved in accordance with the method of undetermined coefficients.

The particular integral i_1 of equation 5 is sought under the form

$$i_1=A\cos(X_f+\omega t)+B\sin(X_f+\omega t)+C \tag{6}$$

where coefficients A, B, and C can be evaluated by substituting the particular solution 6 into equation 5 and evaluating A, B, and C so that an identity is obtained.

Thus we have

$$-\omega LA\sin(X_f+\omega t)+\omega LB\cos(X_f+\omega t)+RA\cos(X_f+\omega t)+RB\sin(X_f+\omega t)+RC\equiv E_m\sin(X_f+\omega t)-(E_g+E_t)$$

Hence, the particular integral of equation 5 is

$$i_1=\frac{RE_m}{R^2+\omega^2L^2}\sin(X_f+\omega t)-\frac{\omega LE_m}{R^2+\omega^2L^2}\cos(X_f+\omega t)-\frac{E_g+E_t}{R} \tag{12}$$

The general solution of equation 5 is found as the sum of equation 12 and of the general integral i_2 of the simplified equation

$$L\frac{di_2}{dt}+Ri_2=0 \tag{13}$$

Equation 13 can be solved directly by separating the variables

$$L\frac{di_2}{i_2}=-Rdt \quad (i_2\neq 0)$$

$$L\log i_2=-RT+M$$

$$i_2=\epsilon^{-\frac{Rt+M}{L}}$$

where M is an arbitrary constant of integration or

$$i_2=D\epsilon^{-\frac{R}{}} \tag{14}$$

where $D=\epsilon^{-\frac{M}{L}}$ is a constant of integration.

The general solution of equation 5 can now be expressed as

$$i=i_1+i_2=\frac{RE_m}{R^2+\omega^2L^2}\ \sin\ (X_f+\omega t)-\frac{\omega LE_m}{R^2+\omega^2L^2}\ \cos\ (X_f+\omega t)-\frac{E_g+E_t}{R}+D\epsilon^{-\frac{R}{L}t} \tag{15}$$

The constant of integration D can be evaluated from the following boundary condition:

$t=0$

$i=0$ (compare equation 2)

Thus

$$\frac{RE_m}{R^2+\omega^2L^2}\ \sin X_f-\frac{\omega LE_m}{R^2+\omega^2L^2}\ \cos X_f-\frac{E_g+E_t}{R}+D=0$$

$$D=\frac{\omega LE_m}{R^2+\omega^2L^2}\ \cos X_f-\frac{RE_m}{R^2+\omega^2L^2}\ \sin X_f+\frac{E_g+E_t}{R} \tag{16}$$

Substituting expression 16 into the equation 15, and taking into account the following well-known relationships:

$$R^2+\omega^2L^2=Z^2 \tag{17}$$

$$\frac{R}{Z}=\cos\theta \tag{18}$$

where Z is the impedance and θ the power-factor angle of the armature circuit at line frequency, we get after a simple transformation the following expression for the armature current:

$$i=\frac{E_m}{Z}\ \sin(X_f-\theta+\omega t)-\frac{E_g+E_t}{R}+\left[\frac{E_g+E_t}{R}-\frac{E_m}{Z}\ \sin\ (X_f-\theta)\right]\epsilon^{-\frac{R}{L}t} \tag{19}$$

Further, denoting

$$\frac{E_g+E_t}{E_m}=a \quad \text{(called "speed factor")} \tag{20}$$

and taking into account the relationship

$$\frac{R}{L}=\frac{\omega}{\tan\theta} \tag{21}$$

as well as equations 2 and 18, we obtain the following final form of the expression for the armature current as a function of angle x:

$$i=\frac{E_m}{R}\left\{\cos\theta\ \sin(x-\theta)-a+[a-\cos\theta\ \sin\ (X_f-\theta)]\epsilon^{-\frac{x-X_f}{\tan\theta}}\right\} \tag{22}$$

In order to analyze and to limit the domain of definition of the variable x and its function i in accordance with the physical property of unidirectional conduction (see circuit in Figure 13), it must be remembered that on account of rectification i cannot assume negative values, that is, the following condition must be fulfilled:

$$i\geqq 0 \tag{23}$$

We further restrict our considerations to the case where the current flows in discreet pulses, that is, two consecutive pulses of current corresponding to two consecutive half-cycles of a single-phase full-wave rectifier do not join with each other. It should be noted that X_f itself is subject to definite limitations which will be discussed in part B. It is useful to note that in line with these limitations it is sufficient to analyze but one current pulse and thus restrict the considerations to a half-wave rectifier system as shown in Figure 13.

Under these conditions the current will twice assume the value 0 within one half-cycle of the applied voltage:

1. For $t=0$, that is, $x=X_f$ when the rectifier starts conducting
2. For $t=t_s$ when the rectifier stops conducting

The instant 1 corresponds to the firing angle $x=X_f$ (see relationship 2). The instant 2 corresponds to an angle X_s such that

$$X_s=X_f+\omega t_s \tag{24}$$

which will be called "stopping" angle of the rectifier.

Thus, t in equation 19 and x in equation 22 are limited in the following manner:

$$\left.\begin{array}{l}0\leqq t\leqq t_s\\ X_f\leqq x\leqq X_s\end{array}\right\} \tag{25}$$

In accordance with limitations 25 expression 22 represents the equation of a single pulse of current.

The equation 22 may be written in the form

$$\frac{iR}{E_m}=\cos\theta\ \sin(x-\theta)-a+[a-\cos\theta\ \sin(X_f-\theta)]\epsilon^{-\frac{x-X_f}{\tan\theta}} \tag{26}$$

which represents the ratio of the instantaneous resistive armature voltage drop to the peak value of the a-c voltage applied to the system as a function of the angle x for given values of parameters a ("speed factor") and θ.

Part B. Firing (Starting) and Stopping Angles of Current Pulses

From the definition of the stopping angle (see equation 24), it follows that $i=0$ for $x=X_s$ and we have from equation 22 the basic relationship between the firing and stopping angles

$$\cos\theta\ \sin(X_s-\theta)-a+[a-\cos\theta\ \sin(X_f-\theta)]\epsilon^{-\frac{X_s-X_f}{\tan\theta}}=0 \tag{27}$$

or after a simple rearrangement

$$[a-\ \cos\theta\ \sin\ (X_s-\theta)]\epsilon^{\frac{X_s}{\tan\theta}}=[a-\ \cos\ \theta\sin\ (X_f-\theta)]\epsilon^{\frac{X_f}{\tan\theta}} \tag{28}$$

The relationship 27 (or 28) represents X_s as an implicit function of X_f for different values of a and θ which are parameters. It should be remembered that

$$\theta=\text{arc tan}\ \frac{\omega L}{R} \tag{21a}$$

determines the relationship between the inductance and the resistance of the armature circuit and

$$a=\frac{E_g+E_t}{E_m} \tag{20}$$

is a term which may be regarded as representing the speed of the motor.

It should be noted that for an ideal rectifier $E_t=0$ and

$$a=\frac{E_g}{E_m} \tag{20a}$$

Although it is impossible to represent the stopping angle X_s in the form of an explicit formula

$$X_s=F(X_f,\theta,a)$$

it is possible to calculate the value of X_s for each value of X_f and different values of parameters a and θ and draw a system of graphs of the function 27 (Figure 4).

In our considerations we restrict ourselves to the physically possible range of the firing angles of the rectifier. On the other hand, as it was mentioned previously, only conditions for discreet pulses of current are being analyzed.

The two previous limitations can be readily represented analytically as well as graphically in the following manner:

If we refer to Figure 14, and remember the fundamental property of the rectifier, namely that conduction is possible only when the anode is positive with respect to the cathode, it will become apparent that the firing angle X_f must satisfy the following condition:

$$x_1\leqq X_f\leqq x_2 \tag{29}$$

Expressions for x_1 and x_2 can be obtained from the equation for the instantaneous armature voltage drop

$$e_a=E_m\ \sin x-(E_g+E_t) \tag{30}$$

from the following condition:

For $x=x_1$ $e_a=0$

Hence

$$E_m\ \sin x_1-(E_g+E_t)=0$$

and

$$x_1=\text{arc sin}\frac{E_g+E_t}{E_m}=\text{arc sin}\, a \tag{31}$$

The expression 31, of course, yields an infinite number of values for x_1, but the physical conditions limit them as follows:

$$x_1=\text{arc sin}\, a,\quad 0\leqq x_1\leqq\frac{\pi}{2} \tag{32}$$

$$x_2=\pi-x_1 \tag{33}$$

For an ideal rectifier $E_t=0$ and a is represented by formula 20a. Here the range of a is as follows:

$$0\leqq a\leqq 1 \tag{34}$$

Obviously, for a stalled motor

$$E_g=0;\ a=0;\ x_1=0;\ x_2=\pi \tag{35}$$

For the maximum possible theoretical speed of the motor

$$E_g=E_m;\quad a=1;\quad X_f=X_s=x_1=x_2=\frac{\pi}{2} \qquad (36)$$

The condition 36 is represented graphically in Figure 4 by a single point $X_s=X_f=90$ degrees.

The border case of the maximum possible firing angle $X_f=x_2$ for $0 \leqq a \leqq 1$ is represented by a straight line A–C connecting points (90°, 90°) for $a=1$ and (180°, 180°) for $a=0$.

It should be noticed that in the case $X_f=x_2$ we always have

$$X_s=X_f=x_2 \qquad (37)$$

and thus for a particular a and $X_f=x_2$ a single point is obtained on graph Figure 4 for all possible values of $\cos\theta$, that is, all curves $X_s=f(X_f)$ for different $\cos\theta$ and the same value of a converge at a point lying on the straight line (90°, 90°), (180°, 180°) and corresponding to that particular value of a.

Since a given a determines the minimum possible firing angle x_1 (see equation 32), each family of curves $X_s=f(X_f)$ for different $\cos\theta$ and the same value of a will stop at a straight line $X_f=x_1=\text{const}$, corresponding to that particular value of a.

It is apparent that for $\cos\theta=1$ and a given a

$$X_s=x_2=\text{const} \qquad (38)$$

If we assume $X_f=x_1$ (minimum possible firing angle) then from equations 38 and 33 we get

$$X_s=\pi-X_f \qquad (39)$$

The preceding equation represents a straight line A–B (90°, 90°), (0°, 180°) shown in Figure 4.

It can be readily understood that in the case of a single-phase full-wave rectifier the necessary condition for a pulsating noncontinuous current is as follows:

$$X_s<180°+X_f \qquad (40)$$

The equation

$$X_s=180°+X_f \qquad (41)$$

represents the border condition when separate successive pulses of current join with one another and is shown in Figure 4 as the straight lines B–D separating the region of pulsating current from that of continuous current.

It may be interesting to note that, with exception of formulas 40 and 41, all the preceding formulas and considerations hold for any multiphase-rectifier system as long as discreet pulses of current are obtained. The necessary condition for a pulsating current represented by expression 40 in case of a single-phase full-wave rectifier system will be modified in the following manner:

Three-phase half-wave rectifier system

$$X_s<120°+X_f \qquad (42)$$

Three-phase full-wave rectifier system

$$X_s<60°+X_f \qquad (43)$$

Six-phase rectifier system

$$X_s<60°+X_f \qquad (44)$$

Four-phase rectifier system

$$X_s<90°+X_f \qquad (45)$$

If for any of the preceding systems the corresponding condition 40, 42, 43, 44, or 45, is fulfilled, the system of curves $X_s=f(X_f)$ shown in Figure 4 can be applied directly to any of these rectifier systems.

The limitation 34 corresponds to the case of an ideal rectifier whose forward resistance is equal to zero (no voltage drop across the rectifier, $E_t=0$). For an actual mercury vapor or gaseous discharge rectifier, however, formula 20 must be used rather than 20a since

$$E_t \cong 15 \text{ to } 20 \text{ volts}$$

and limitation 34 will become

$$k \leqq a < 1 \qquad (46)$$

where $k \cong 20/E_m$ corresponds to the condition of a stalled motor (see equations 20 and 20a) and is a low number of the order of magnitude of 0.04.

Part C. Peak Current and Its Angle of Occurrence

In order to determine the crest value of the current pulse we refer back to the equation 26.

Differentiating equation 26 with respect to x we find

$$\frac{R}{E_m}\frac{di}{dx}=\cos\theta\ \cos(x-\theta)-\frac{1}{\tan\theta}\times [a-\cos\theta\sin(X_f-\theta)]\epsilon^{-\frac{x-X_f}{\tan\theta}} \qquad (47)$$

The maximum of the armature-current pulse will occur at the angle $x=X_p$ such that

$$\cos\theta\cos(X_p-\theta)-\frac{1}{\tan\theta}\times [a-\cos\theta\ \sin(X_f-\theta)]\epsilon^{-\frac{X_p-X_f}{\tan\theta}}=0 \qquad (48)$$

or after a simple rearrangement

$$\cos(X_p-\theta)\epsilon^{\frac{X_p}{\tan\theta}} =\left[\frac{a}{\sin\theta}-\frac{\sin(X_f-\theta)}{\tan\theta}\right]\epsilon^{\frac{X_f}{\tan\theta}} \qquad (49)$$

Equation 48 or 49 represents the relationship between the peak-current angle X_p and the firing angle X_f, that is, represents X_p as an implicit function of X_f for different values of parameters a and θ. Similarly, as in case of functions 27 or 28 it is possible to calculate the value of X_p for each value of X_f (see limitation 29), although X_p cannot be represented by an explicit formula. The graphs of the function $X_p=f(X_f)$ for different values of parameters a and θ are shown in Figure 6.

If symbol X_p is substituted for x into the equation 22, we will get the expression for the peak value of the current pulse $i_{\max}$.

$$i_{\max}=\frac{E_m}{R}\left\{\cos\theta\ \sin(X_p-\theta)-a+ [a-\cos\theta\ \sin(X_f-\theta)]\epsilon^{-\frac{X_p-X_f}{\tan\theta}}\right\} \qquad (50)$$

where

$$X_p=f(X_f)$$

as represented by equation 48 or 49.

A more convenient form analogous to equation 26 is

$$\frac{i_{\max}R}{E_m}=\cos\theta\sin(X_p-\theta)-a+ [a-\cos\theta\sin(X_f-\theta)]\epsilon^{-\frac{X_p-X_f}{\tan\theta}} \qquad (51)$$

Graphs of the function 51 for different values of parameters a and θ are shown in Figure 7.

It is useful to note that, for the theoretical case of zero inductance $\cos\theta=1$, neither X_p nor $i_{\max}$ can be calculated from the equations 49 and 50 which assume an indeterminate form for $\theta=0$. However, from Figure 14 it is apparent that for $\theta=0$ we have

$$\left.\begin{array}{ll} X_p=X_f & \text{when } X_f\geqq 90° \\ X_p=90° & \text{when } X_f<90° \end{array}\right\} \qquad (52)$$

and

$$\left.\begin{array}{ll} \dfrac{i_{\max}R}{E_m}=\sin X_f-a & \text{when } X_f\geqq 90° \\[2ex] \dfrac{i_{\max}R}{E_m}=1-a & \text{when } X_f<90° \end{array}\right\} \qquad (53)$$

Graphs of equations 52 and 53 are also shown in Figure 6 and Figure 7.

Part D. Armature Load-Speed Relations

Having established the relationship between the firing and stopping angles of the rectifier (see Figure 4), we now take up the problem of determining the average value of the resistive voltage drop in the armature circuit $I_{\text{avg}}R$ proportional to the average armature current, that is, proportional to the torque of the motor.

In order to obtain the average value of the armature current I_{avg} or the average value of the ratio $I_{\text{avg}}R/E_m$, one should integrate the function 22 or 26 between the limits $x=X_f$ and $x=X_s$, taking the average over one half-cycle of the applied voltage in case of single-phase full-wave rectification. Thus

$$I_{\text{avg}}=\frac{1}{\pi}\frac{E_m}{R}\int_{x=X_f}^{x=X_s}\left\{\cos\theta\ \sin(x-\theta)-a+ [a-\cos\theta\ \sin(X_f-\theta)]\epsilon^{-\frac{x-X_f}{\tan\theta}}\right\}dx \qquad (54)$$

However, it can be readily proved by actually performing the integration in equation 54 and taking into account the relationship 27 that the same result will be obtained in a much simpler way by integrating the voltage function rather than the current one.

With reference to Figure 14, it can be easily understood that the equation for the instantaneous armature voltage drop is

$$e_a=E_m\sin x-(E_g+E_t) \qquad (30)$$

We obtain the average resistive voltage drop in the armature circuit by integrating the function 30 between the limits $x=X_f$

Electronic Control of D-C Motors

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IN recent years the need for an improved adjustable-voltage d-c motor drive operated from an a-c supply has been continually increasing. Although the conventional drive of that type using a motor–generator set, and sometimes referred to as the Ward-Leonard system, adapts a d-c motor to an a-c line and provides adjustable armature voltage for the motor, it does not solve all of the problems involved. Thus, good speed regulation at all speeds, ability to deliver full torque at speeds as low as $^1/_{50}$ of the rated speed of the motor, and automatically controlled acceleration and reversal are but a few vital problems which have been awaiting solution.

Since the development of mercury-vapor and inert-gas-filled tubes with controlled angle of ignition, such as thyratron and ignitron rectifiers, numerous attempts were made to apply them for armature control of d-c motors. Here, the firing angle of the rectifier can be controlled to vary the rectifier output voltage applied to the armature. Also, an additional controlled rectifier may be used to supply field current to extend the range of control above the rated or base speed of the motor.

Rectifiers for armature control of d-c motors were first used about ten years ago, but until recently that kind of drive has not been of major importance, primarily because of its excessively drooping speed-torque characteristic.

Recent engineering development, however, has improved the performance of the rectifier–motor system so that it has become one of the most perfected drives. It possesses a very good speed regulation throughout an extremely wide speed range and also incorporates a number of additional features such as fully automatic control of acceleration, deceleration and reversal of the motor.

An accompanying paper by E. H. Vedder and K. P. Puchlowski, "Theory of Rectifier–D-C Motor Drive,"[1] presents the mathematical analysis and a number of conclusions concerning the rectifier–motor system in general. This paper deals with certain specific characteristics and circuits of a modern rectifier motor drive and its advantages as compared to an elementary unregulated system.

Elementary System

The schematic circuit diagram of an elementary rectifier–motor system is shown in Figure 1. Thyratron tubes 1 and 2 are connected as a conventional single-phase full-wave rectifier and supply unidirectional voltage to the armature of the motor connected between the cathodes and the center tap of the rectifier transformer. The a-c grid voltage applied to the grids of the tubes from the control-grid transformer is shifted to lag in phase the anode supply voltage of the thyratrons by 90 degrees. The voltage applied to the primary winding of the control grid transformer is obtained from a conventional dephasing circuit consisting of a resistance $R5$ and a capacitance $C5$.

The grid voltage of thyratron tubes 1 and 2 also includes a d-c component which is a combination of two voltages across portions of $P1$ and $P5$, so that the a-c component is superimposed on the d-c voltage. This d-c voltage may be varied from a definite negative to a definite positive value, and in that way the firing angle of the tubes 1 and 2 may be adjusted arbitrarily by changing the setting of the potentiometer $P1$.

Although the speed of the motor may be varied by the adjustment of the speed-control potentiometer $P1$, the speed not only depends on the setting of that potentiometer but is affected to an overwhelming degree by the mechanical torque on the shaft of the motor. A particular setting on the speed-control dial does not in fact correspond to any particular speed which may be anything from zero to full speed of the motor, depending upon the value of the load torque.

In order better to understand the operation and performance of a rectifier–motor system, it seems useful to refer back to the characteristics of a conventional d-c shunt-wound motor.

It is a well-known fact that a conventional shunt-wound motor normally has a slightly drooping speed-torque characteristic which may be represented by the following equation:

$$n=\frac{E-IR}{C\phi}$$

where

n = number of revolutions per minute
E = d-c voltage across the armature
I = armature current
R = resistance of armature circuit
ϕ = main magnetic flux
C = coefficient of proportionality

Under normal conditions, E is constant, ϕ and R may be assumed constant, and the preceding equation will represent n as a linear function of I (or torque); see Figure 2. The speed of the motor will decrease with increasing torque because of the effect of the armature voltage drop which, assuming a constant magnetic flux ϕ, is directly proportional to the torque. The change in speed obviously

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and $x=X_s$ and taking the average over one half-cycle of the applied voltage.

$$I_{avg}R=\frac{1}{\pi}\int_{x=X_f}^{x=X_s} e_a dx$$

$$I_{avg}R=\frac{1}{\pi}\int_{x=X_f}^{x=X_s}[E_m \sin x-(E_g+E_t)]dx \tag{55}$$

Thus

$$I_{avg}R=\frac{1}{\pi}\left\{E_m \Big/ -\cos x \Big/_{x=X_f}^{x=X_s} - (E_g+E_t) \Big/ x \Big/_{x=X_f}^{x=X_s}\right\}=\frac{1}{\pi}[E_m\times (\cos X_f-\cos X_s)-(E_g+E_t)(X_s-X_f)] \tag{56}$$

Taking into account the relationship 20, we may write the expression 56 in the following form:

$$I_{avg}=\frac{1}{\pi}\frac{E_m}{R}[\cos X_f-\cos X_s-a(X_s-X_f)] \tag{57}$$

or

$$\frac{I_{avg}R}{E_m}=\frac{1}{\pi}[\cos X_f-\cos X_s-a(X_s-X_f)] \tag{58}$$

where $I_{avg}R/E_m$ is called the "torque factor."

Graphs of the equation 58 for different values of a and $\cos\theta$ are shown in Figure 8 and Figure 9.

Reference

1. ELECTRONIC CONTROL OF D-C MOTORS, K. P. Puchlowski. AIEE TRANSACTIONS, volume 62, 1943, pages 870–7.

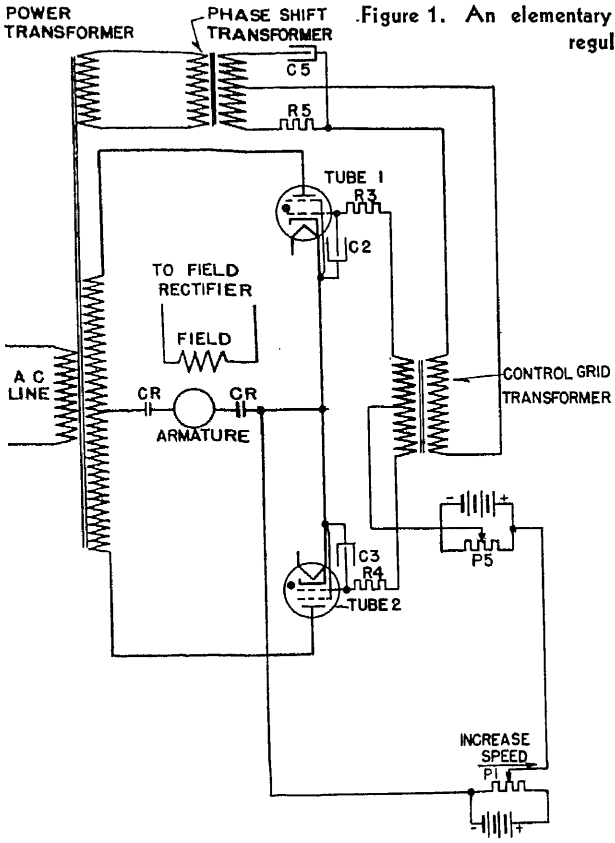

Figure 1. An elementary rectifier drive without speed-regulating circuit

depends upon the armature resistance and usually at rated speed will amount to say 8 to 12 per cent, with torque varying from no-load to full-load value. At lower speeds the percentage of speed regulation is higher, since, for lower values of E, IR constitutes a higher percentage of the armature voltage, and its influence is much more pronounced. The speed–load-current characteristics for a conventional d-c shunt-wound one-horsepower motor are shown in Figure 2.

However, in a rectifier system like the one represented in Figure 1, the interpretation of the speed–load equation must be properly modified. In the first place, neither the armature voltage E nor current I are straight d-c values any more, and one has to deal with the concept of average values. Also, the average value of voltage E across the armature is not constant any more, as it was in the case of a conventional d-c drive. When the speed-control potentiometer is set at a definite position, that is the firing angle of the armature-control thyratrons is constant during the time when torque is varied from no-load to full-load value, E itself becomes a function of torque, and in this way speed constitutes a much more involved function of I than that represented by the speed–load equation and graphs in Figure 2.

In order to clarify the meaning of the concept of armature voltage with reference to a rectifier–motor system, we refer to Figure 3, where the a-c anode supply voltage, the armature voltage, the voltage across the rectifying element, and the control grid voltage are represented as functions of time (or electrical angle). For the sake of simplification of the diagram, the latter has been drawn as for a single-phase half-wave system. For a single-phase full-wave as well as any multiphase rectifier, the considerations will be analogous. In order further to simplify the diagram, it has been assumed that the critical grid voltage of the thyratron tube is equal to zero, that is, the tube will break down at the point of intersection of the grid-voltage wave with the line of cathode potential of the tube. Let it be assumed further that the motor is stalled first and thus will behave as an ordinary resistive-inductive load.

Under these conditions, it is obvious that the thyratron will break down at the point T and will conduct current past point C up to the point M. The current pulse will have a shape as shown in Figure 3. The magnitude of the current may be very high here and will depend

1. Upon the firing angle of the thyratron tube.
2. Upon the armature resistance and inductance.

It should be noted in passing that by reason of the armature inductance the current will not stop flowing at the point C where the anode supply voltage passes through zero, since the inductive electromotive force of the winding will reverse its polarity and will tend to maintain the flow of current opposing the a-c supply voltage. Obviously, the current will stop flowing at the instant when the inductive voltage drop becomes equal to the applied a-c voltage.

After the motor has started running, and its speed has been established, the picture becomes entirely different. Now, a counter electromotive force is generated in the armature winding and, under steady-state conditions, represents a constant d-c voltage which may be assumed to be in direct proportion to the speed of the motor (under the assumption of constant magnetic flux). This counter-electromotive force E_g represented by the line O' in Figure 3 acts in the direction to oppose the flow of current and, consequently, to limit its value. The thyratron will break down at the point H (same instant as far as time is concerned) and will conduct the pulse of current up to the point P. Although, the counter-electromotive force cannot produce any current in the armature circuit (being blocked out by the rectifying elements), it actually will appear across the armature terminals over the period of time when the rectifier is not conducting and, contrary to a conventional d-c system, will overwhelmingly affect the reading of a d-c voltmeter connected across the armature. The armature voltage will have the shape as shown by a heavy line $O'GHKLNPQ$ . . . with respect to the zero line O (potential of the negative brush). The armature current will flow over a portion of the corresponding half-

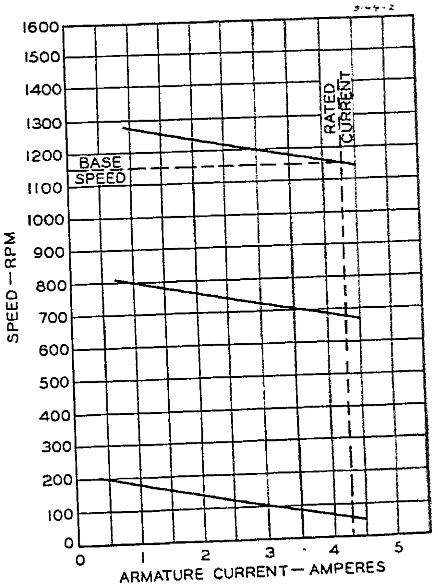

Figure 2. Speed-torque curves of a standard shunt-wound d-c motor supplied with direct current

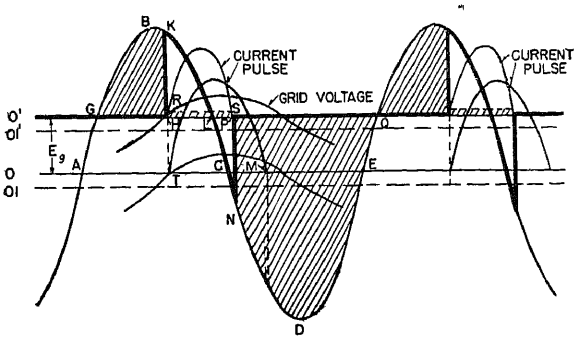

Figure 3. The voltage and current relations in a motor whose armature is supplied from a half-wave rectifier at zero and at approximately full speed

cycle actually much smaller than that for the stalled motor. The average value of the armature voltage drop *IR* which is proportional to the difference of the areas *HKL* and *LNP* and, consequently, the average value of current will be, of course, much smaller than that obtained for a stalled motor. Obviously, they both are determined by the torque of the motor.

If the load torque of the motor is increased, the armature current will also increase, as well as the *IR* drop, and it can easily be seen from Figure 3 that for the same firing angle of the thyratron tube the counter-electromotive force of the motor will have to decrease, and, obviously, speed will have to decrease. A very important point, however, is that the average value of voltage across the armature will also decrease, since this voltage will vary with counter-electromotive force as it can plainly be seen from Figure 3, instead of remaining constant as is the case for a conventional d-c drive. It must be emphasized that for any practical set of conditions the decrease in armature voltage *E* with increased torque will be very high. Thus, with reference to the speed–load equation, it is readily seen that, with increasing torque, not only will the second term of the numerator increase, but contrary to a conventional situation, the first term will decrease rapidly so that the revolutions per minute of the motor will drop at an extremely high rate. The rate at which the speed of the motor will drop with increasing torque, assuming a constant angle of ignition, will be highest for a single-phase half-wave rectifier system and the speed regulation in general will improve for a single-phase full-wave rectifier and will further improve with additional increase of number of phases.

Figure 4 shows curves of the armature voltage as a function of armature current for a one horsepower 230 volt d-c motor driven from a single-phase full-wave rectifier. The rectifier was connected in a system as shown in Figure 1, and constant angle of ignition was maintained throughout the test. The corresponding speed–armature-current characteristics of the same motor are shown in Figure 5. This striking behavior of a rectifier drive (compare Figure 2 for a conventional d-c drive) makes it clear enough why these drives in their elementary form have not acquired much practical importance.

Circuit for Improved Speed Regulation

As it was pointed out previously, there are two main reasons for an extremely poor speed regulation of the elementary rectifier drive for d-c motors:

1. Armature voltage drop *IR*.
2. Consequent decrease in the armature voltage *E* with increasing torque.

The latter reason is of prime importance and has a predominant effect on the behavior of a rectifier drive.

The situation can be substantially changed if the firing angle of the rectifier tubes (thyratrons or ignitrons) is adjusted automatically in accordance with torque variations, that is, advanced with increasing torque and delayed with decreasing torque so as to maintain constant speed for any particular setting of the speed-control potentiometer.

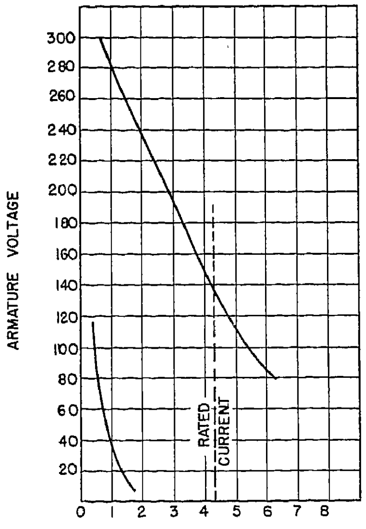

Figure 4. Average armature voltage as a function of load when armature is supplied from a single-phase full-wave rectifier for two different firing angles

In order to provide means for automatic adjustment of the angle of ignition so as to make the speed of the motor independent of torque, a speed-indicating device or arrangement is necessary. This indicating device must provide a certain physical quantity, voltage for example, which is directly proportional to the speed and will influence the control circuits properly in such a direction as to restore the speed of the motor to its previous value. The commonly known tachometer generator is one of the possible indicating means. It is usually mounted on the shaft of the motor and generates a voltage directly proportional to the revolutions per minute of the motor. However, an arrangement like that has a number of disadvantages, such as the necessity of having a special motor with provisions for mounting the tachometer generator, not to mention additional cost, space, and maintenance involved. Consequently, engineering development recently has been directed toward the solution of the problem of speed indication in a manner which might avoid the use of any tachometer generator.

A circuit with a feedback from the armature voltage constitutes a definite improvement over the scheme shown in Figure 1, since the voltage across the armature can be maintained constant so that the main cause of poor speed regulation will be eliminated (see Figure 4 and Figure 5). In Figure 6 is shown a schematic circuit diagram of a rectifier drive for a d-c motor with substantially improved speed regulation resulting from the fact that at varying load the voltage across the armature automatically is maintained constant at a value corresponding to a particular setting of the speed-control potentiometer.

The circuit in Figure 6 consists of a

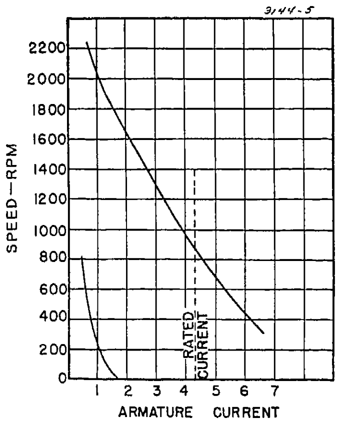

Figure 5. Speed-torque curve when armature is supplied from a single-phase full-wave rectifier for two different firing angles

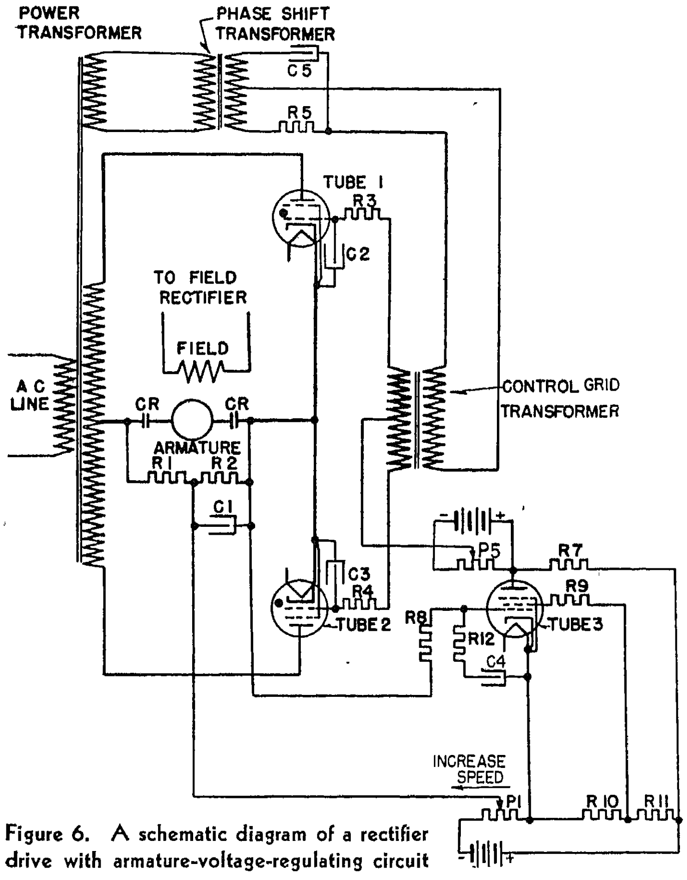

Figure 6. A schematic diagram of a rectifier drive with armature-voltage-regulating circuit

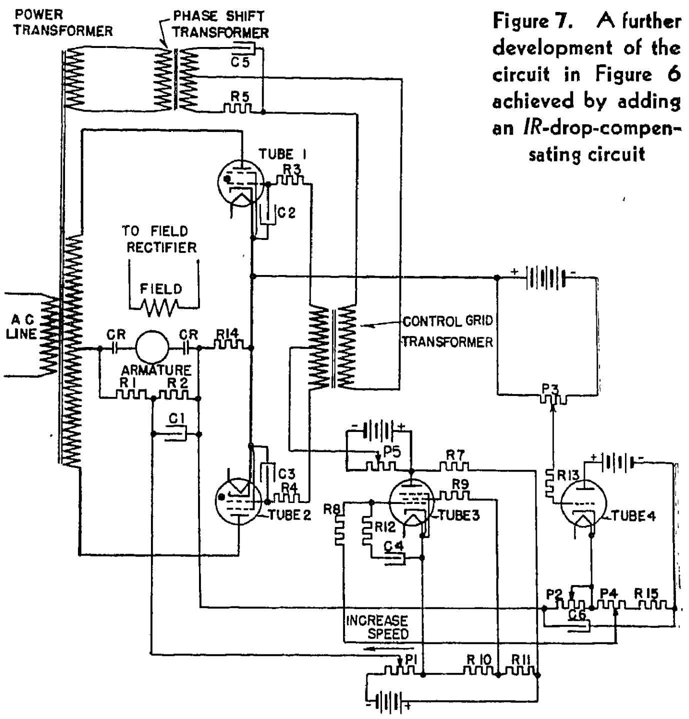

Figure 7. A further development of the circuit in Figure 6 achieved by adding an *IR*-drop-compensating circuit

single-phase full-wave rectifier system comprising the power transformer, thyratron tubes 1 and 2, the armature winding of the motor, and the speed-control circuit comprising the control tube 3 and its allied circuits. For the sake of simplification, auxiliary sources of d-c voltage are designated as batteries. As in the circuit shown in Figure 1, the voltage applied to the control grids of the thyratrons 1 and 2 consists of an a-c component superimposed on the d-c component. The a-c grid voltage component is obtained through the medium of the control grid transformer from a phase-shift circuit and lags the anode supply voltage by 90 degrees. The variable d-c component is developed in the following portion of the thyratron grid circuit: cathode—*R*2—portion of *P*1—*R*10—*R*11—*R*7—portion of *P*5—center tap of the secondary winding of the control grid transformer. Voltages across *R*2 and the active portion of the potentiometer *P*1 may be disregarded here since they are low as compared with voltages across *R*10, *R*11, *R*7, and *P*5, and since they practically cancel each other. The sum of d-c voltages across *R*10, *R*11, and *P*5 has a constant value whereas the voltage across *R*7, that is, the load voltage of the control tube 3 varies in proportion to the plate current of the preceding tube. This variable voltage determines the value of the d-c component of the thyratron grid voltage and, consequently, the firing angle of rectifiers 1 and 2. The foregoing combination of voltages is designed in such a way that, when the plate current of tube 3 is equal to zero, the thyratron tubes will supply maximum voltage to the armature of the motor. When the plate current of tube 3 is maximum, no voltage is supplied to the armature by thyratron tubes 1 and 2.

The control grid circuit of tube 3 can be traced as follows: cathode—active portion of *P*1—*R*2—*R*8—control grid. The voltage across the active portion of the speed-control potentiometer *P*1, is controlled arbitrarily, simply by turning the knob of that potentiometer. The voltage across *R*2 is proportional to the armature voltage of the motor and represents the feed-back voltage of the system. It should be noted that, although the armature voltage contains a very sharp ripple as plainly seen in Figure 3, a proper filtering effect is secured by means of circuit *R*1—*R*2—*C*1, so that the voltage across *R*2 does not contain any appreciable ripple.

When the motor is running at a certain speed, the voltage across the active portion of the speed-control potentiometer *P*1 and the voltage of opposite polarity across *R*2 will be balanced against each other in such a way that a resulting negative voltage of a few volts will be applied to the control grid of tube 3. The aforementioned grid voltage will correspond to a definite voltage across *R*7 and thus to a definite firing angle of tubes 1 and 2. However, if the slider of *P*1 is moved away from the cathode of tube 3, this balance will be momentarily disturbed. The control grid of tube 3 will now become more negative; the voltage across *R*7 will decrease, thus advancing the firing angle of the thyratron tubes. This will result in an increase in armature voltage and, consequently, in speed of the motor. The armature voltage and the speed will increase to such a degree that a new balance of the system will be established at a slightly more negative resultant grid voltage of tube 3. Thus, it can readily be seen that the speed of the motor will increase when the slider of *P*1 is moved away from the cathode of tube 3 and will decrease when *P*1 is turned toward the cathode.

Furthermore, it is apparent that the feed-back arrangement incorporated into the circuit Figure 6 will eliminate the variations of armature voltage with load and at the same time stabilize the operation of the system. Supposing that the torque applied to the shaft of motor is being increased, and in accordance with previous considerations the armature voltage tends to decrease, it is obvious that the grid of tube 3 will become slightly more negative just enough to advance the firing angle of thyratron tubes in such a way as to maintain the voltage across the armature at an essentially constant value. It is important to remember that

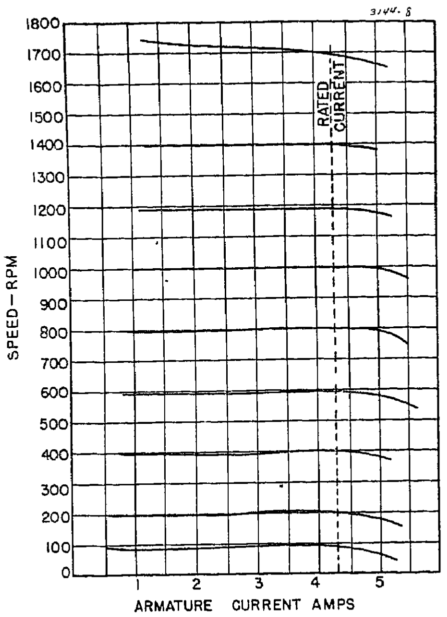

Figure 8. Speed-torque curves of the improved electronic drive

the control tube 3 provides the required high degree of amplification so that a change in the armature voltage amounting to a fraction of one volt will result in an appreciable shift of the angle of ignition of the thyratron tubes.

IR-Drop Compensation

The voltage across the armature is not a very accurate indication of speed, on account of the armature voltage drop *IR* which, even at higher speeds, may result in about ten per cent speed regulation and at lower speeds has a still greater effect on variation of speed with load.

In order to eliminate the influence of the armature voltage drop on the speed-torque characteristic of the drive, the schematic circuit diagram Figure 6 can be developed further by the addition of an *IR* compensating circuit. The feedback for this compensating circuit is taken from the armature current, and the firing angle of the rectifier tubes is additionally adjusted in accordance with load variations in such a way that the armature voltage will increase with load, providing proper compensation for the armature voltage drop.

A simplified schematic circuit diagram incorporating an armature-voltage-drop compensating circuit is shown in Figure 7.

As it can readily be seen, the diagram shown in Figure 7 has been derived from that shown in Figure 6 by the addition of another amplifier tube 4—high vacuum triode—and its allied circuits. The designations of circuit elements common to circuits in Figure 7 and Figure 6 are the same. A new resistor *R*14 has been introduced in series with the armature winding of the motor. This additional resistance amounts to only a fraction of an ohm and serves the purpose of indicating the load (torque) conditions of the motor. The resistor *R*14 is in the grid circuit of the *IR*-drop-compensating tube 4. Other elements of that grid circuit consist of the antihunting-control rheostat *P*2, bias-control potentiometer *P*3, and the grid resistor *R*13. The compensation-control potentiometer *P*4 and resistor *R*15 constitute the load resistance of tube 4. A portion of the voltage drop developed across *P*4 is introduced into the grid circuit of the master control tube 3, so that this voltage drop tends to make the grid of tube 3 more negative.

Table I

Armature Current (Amperes)	Armature Voltage	Speed (Rpm)
1.00	129.6	1,000
1.45	130.0	996
3.25	133.0	999
3.50	133.6	1,000
4.00	134.2	997
4.30	135.0	1,000
5.00	134.0	993
0.75	53.0	400
1.20	53.9	396
1.80	55.0	393
2.10	56.0	393
2.50	57.4	395
2.90	58.1	396
3.40	60.0	402
3.90	62.0	404
4.50	64.0	401

Referring to the grid circuit of the master control tube 3 (Figure 7), it will be seen that the resultant grid voltage of the aforementioned tube is a combination of three voltage components: the negative component obtained from the speed-control potentiometer *P*1, the positive component across the armature-voltage feed-back resistor *R*2, and the negative component across a portion of the *IR* compensation-control potentiometer *P*4. If it is assumed that the torque of the motor is being increased, the armature current will increase and so will increase the average voltage drop across *R*14, making the grid of tube 4 less negative. Consequently, the plate current of this tube will increase and so will increase the voltage drop across *P*4, making the control grid of the master control tube more negative. In accordance with previous considerations, this will result in an additional advancement of the firing angle of the thyratron tubes 1 and 2, so that the armature voltage will be increased additionally to compensate for the increase in the armature voltage drop.

It should be emphasized that the amount of compensating action can be controlled by means of the potentiometer *P*4, and in that way a complete control of the speed-torque characteristic of the drive is possible. A slight twist of the knob of *P*4 will change the speed-torque characteristic of the drive so as to adapt it to any particular motor or application. Either a drooping or a rising characteristic can be obtained, and a definite optimum setting of *P*4 will result in a practically constant speed for all the values of torque and speed within the normal operating range of the drive.

Performance

In Figure 8 is shown an experimentally obtained family of speed-torque characteristics of the drive with a one horsepower 1,750 rpm 230-volt d-c shunt-wound motor. Full-rated field current was maintained throughout the test. Here, the compensation-control potentiometer *P*4 was set at an optimum setting to give a close speed regulation over a 20:1 speed range. It should be noted that the setting of *P*4 was the same for the entire range of speeds, that is, one single adjustment was sufficient to take care of the armature-voltage-drop compensation for all the speeds available on the dial of the speed-control potentiometer *P*1. From the graphs shown in Figure 8 it may be seen that, within the armature current range from no-load current to full-rated current of 4.3 amperes, the speed of the motor did not vary more than by three

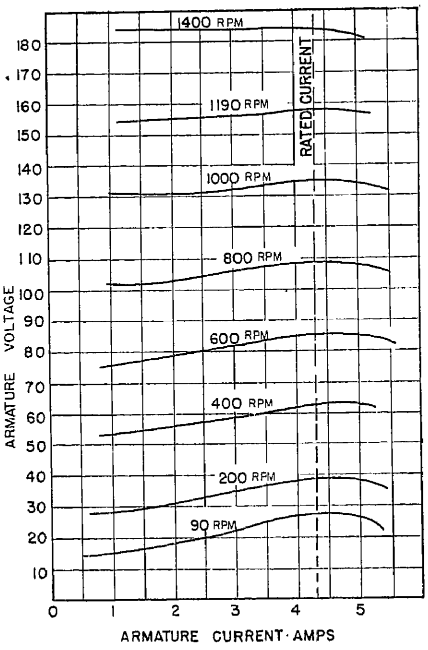

Figure 9. Armature voltage of the improved electronic drive

Compare with Figure 4

per cent for any speed within the speed range from the base (rated) speed of the motor down to $^1/_{10}$ of that value. At $^1/_{10}$ of the base speed, regulation was about 6 per cent. The increased drooping of the speed—load-current characteristics for currents exceeding the rated value of 4.3 amperes was caused by the current-limitation feature of the drive, not included in diagram Figure 7, which will be discussed later.

Experimental graphs of armature voltage versus load current corresponding to speed-current characteristics of Figure 8 are shown in Figure 9. Here, it is plainly seen how the armature voltage is being increased automatically to compensate for increasing armature voltage drop.

Since small changes in speed or in voltage can hardly be detected in graphs Figure 8 and 9, the actual values of speed, armature current, and armature voltage for two different settings of the speed-control potentiometer have been assembled in Table I.

From graphs shown in Figure 8, it is apparent that the motor is able to deliver full-rated torque at a speed as low as $^1/_{10}$ of its rated speed and that this speed is maintained essentially constant over the entire range from no-load to full-load torque.

Another important feature of the drive is that, in accordance with speed–load-current characteristics shown in Figure 8, the speed of the motor depends only on the setting of the speed-control potentiometer, that is, it is possible to come back to a required speed simply by selecting it on the dial of the control.

Automatically Controlled Acceleration of the Motor

The starting and acceleration of a d-c motor constitutes a problem in itself, especially when an automatic acceleration is to be considered. In general, there are two methods by which automatic acceleration can be achieved: the time-delay acceleration and the current-limit acceleration. In the first case the voltage applied to the armature is being increased gradually to its normal value, so that the motor is allowed to start smoothly. The current-limit acceleration is based on the fact that means are provided which do not permit the armature current to rise above a predetermined value. The current-limit method of acceleration has definite advantages in that it enables an acceleration consistent with the kind of load. Thus, the time of acceleration will be different for different loads, depending upon the mechanical inertia of the rotating system, so that starting will always take place under the most advantageous conditions, considering the time and the smoothness of acceleration.

An electronic-control system such as just described is exceptionally well suited to be developed still further, so as to include additional circuits which would perform the function of automatic current-limited acceleration of the motor. A diagram of a complete system incorporating all the features of circuits shown in Figure 6 and Figure 7, as well as performing the additional function of controlling automatically the acceleration, is represented in Figure 10. It will be noticed that a third high-vacuum control tube has been added to the circuit. This tube 5 is a pentode characterized by a sharp cutoff and has a power supply common with tube 3. The grid circuit of tube 5 includes the load-current indicating resistor *R*14 and the biasing potentiometer *P*6. The armature voltage indicating resistor *R*2 is in the plate circuit of tube 5. A closer examination of the circuit will reveal that the action of the current-limiting tube 5 is just opposite to that of the *IR*-drop-compensating tube 4. Thus, in general, an increasing armature current will tend to make the control grid of tube 5 less negative through the influence of the voltage drop across *R*14 and in that way will cause the plate current of tube 5 to increase. This will result in an increase of voltage drop across *R*2, and the control grid of the master control tube 3 will become less negative, so that the plate current of that tube will increase. In accordance with previous considerations, this will mean the delaying of the firing angle of the thyratron tubes 1 and 2.

The circuit is so designed that, within the normal operating range of armature currents, say from zero to full-rated current, tube 5 is inoperative, that is, does not conduct any current being biased off by a highly negative voltage from *P*6. Thus, within that range the armature current affects only the *IR*-drop-compensating tube 4, as it was previously described. If, however, the armature current increases above a definite value determined by the setting of the potentiometer *P*6, tube 5 will start conducting. The tube characteristic and the circuit constants are such that the amplifying action of tube 5 is much stronger than that of tube 4, and, as soon as tube 5 starts conducting, it completely overwhelms the opposing action of tube 3, providing a very strong delaying effect on the firing angle of the armature-control thyratron tubes. Under these conditions it is apparent that, when the armature current increases above a certain value because of an overload, for example, the tube 5 will start conducting, and the firing angle of the thyratron tubes will be delayed which will, of course, oppose the tendency of current to increase. With still increasing load torque, the motor will stall when the current in the armature winding reaches its maximum value, as determined by the setting of the potentiometer *P*6. The current limit is adjustable by means of potentiometer *P*6. In most practical cases it is kept within the range of, say, 1.8 to 2.5 times rated armature current of the motor.

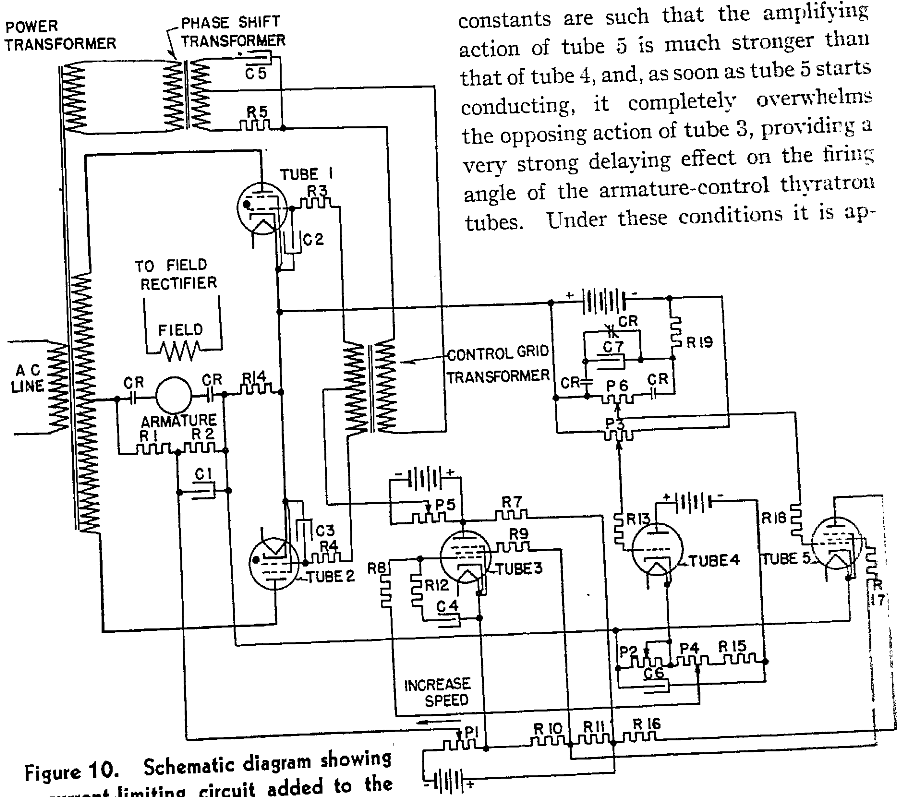

Figure 10. Schematic diagram showing a current-limiting circuit added to the basic circuit in Figure 7

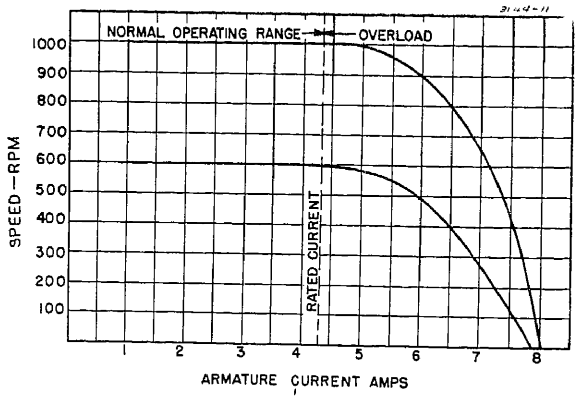

Figure 11. Curves showing the entire range of speed-torque characteristic

Current is held by the limiting circuit to about twice the rated value

In Figure 11 are shown two speed-armature-current characteristics of the drive for two different settings of the speed-control potentiometer taken over the entire range from no load to a maximum current corresponding to a locked armature. Here, the armature current was limited to about twice its rated value.

During the starting of the motor, when armature contactor *CR* is being closed, the current will rise and usually will attain the current limit as determined by the grid conditions of the current-limiting tube 5, that is, by the setting of the current-limit-adjusting potentiometer *P*6.

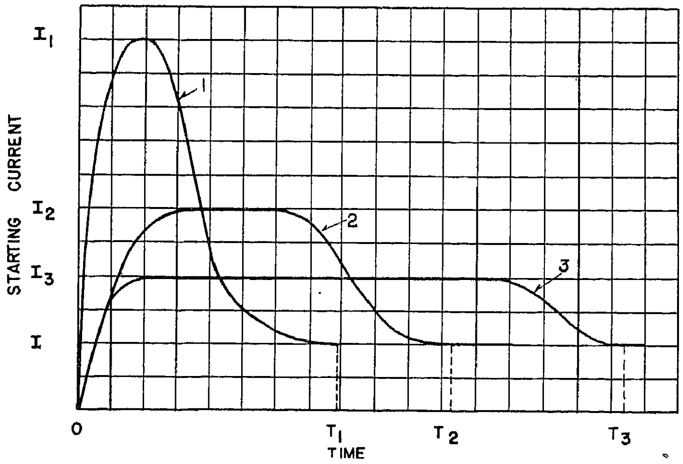

Figure 12. Curves showing the effect of automatic current limitation during acceleration of the motor

Curve 1 shows current inrush in motor operating from a d-c line. Curves 2 and 3 show the motor current limited automatically by the electronic control to three and two times the full load, respectively

In that way it is apparent that the starting torque of the motor will be limited also, and thus a smooth and shockless starting is obtained.

However, since the current-limiting circuit can become operative only after the average current has reached a definite value, it is obvious that the current-limit tube 5 will not affect the angle of ignition at which the first breakdown of the thyratron tubes will occur, immediately after the closure of the armature contactor *CR*. Thus, the first breakdown will take place at a firing angle which is not delayed by the action of tube 5 and is determined solely by the setting of *P*1. Under these conditions the first pulse of armature current may attain a considerable magnitude, being limited only by the resistance and inductance of the armature winding.

If the speed-control potentiometer *P*1 is set for a high speed, the first peak will be usually much higher than the peak-current rating of the armature rectifier tube would tolerate.

In the diagram shown in Figure 10, this disadvantage has been eliminated by a circuit providing an additional time-delaying action during the starting of the motor, so that in fact a mixed current-limit and time-delay acceleration is obtained. The new circuit includes capacitor *C*7, resistor *R*19, two normally open contacts of the contactor *CR*, and one normally closed contact of that contactor. When the armature contactor *CR* is open, it is apparent that the voltage across the capacitor *C*7, as well as across the potentiometer *P*6, is equal to zero. In that way the grid voltage of tube 5 is zero, and the tube is conducting full current, resulting in a high voltage drop across *R*2. Under these conditions the master control tube 3 will conduct full current, regardless of the setting of the speed-control potentiometer *P*1, so that the rectifier tubes 1 and 2 are not allowed to conduct.

At the instant of closure of the armature contactor *CR*, the voltage across *P*6 is still equal to zero, and thyratron tubes 1 and 2 cannot conduct. However, the voltage across the current-limit-control potentiometer *P*6 will rise gradually, following the charging of the capacitor *C*7 through the resistor *R*19. In that way the tube 5 is being biased off gradually, and the firing angle of the thyratron tubes will be advanced gradually, so that the magnitude of the first current pulses will be limited, even before tube 5 responds to the feedback from the armature-current-indicating resistor *R*14. The time delay provided by the capacitor *C*7 is of short duration corresponding to 3–4 cycles only, and the rest of the acceleration is controlled by the tube 5 in accordance with the feedback from *R*14, in a manner as previously described.

The process of automatically controlled acceleration of the motor can be understood best from Figure 12 where the starting current of the motor is plotted as a function of time. The ordinates of the curves 2 and 3 represent the average values of particular current pulses. Curve 1 represents the starting current of a motor with a d-c voltage applied to the armature at the instant *O*. Obviously, the current will not rise instantly because of the inductance of the armature winding. The starting current will quickly reach its peak I_1 and will drop to the value *I* determined by the load following the acceleration of the motor. If the

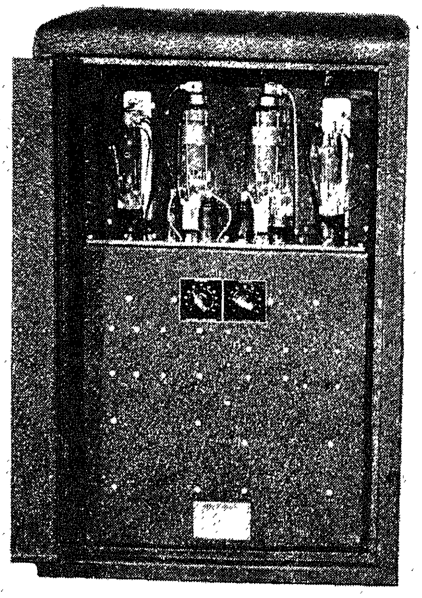

Figure 13. A one-horsepower single-phase electronic drive incorporating accurate speed regulation, *IR*-drop compensation, and current-limited acceleration

inertia of the load is high, this peak value I_1 will be limited only by the resistance of the armature circuit. The total time of acceleration is T_1, as shown on the graph. Curves 2 and 3 represent the average value of starting current as a function of time with the current limit set at three times the rated current I for curve 2 and two times the rated current for curve 3. T_1, T_2, and T_3 represent correspondingly the time of acceleration for each particular curve. It is apparent that, the lower the current limit, the longer will be the time of acceleration and the smoother the starting of the motor. By means of the potentiometer *P*6 (see Figure 10), the current limit can be varied and in that way the starting torque, the time, and smoothness of acceleration can be controlled.

When the armature contactor *CR* is being opened, it can be seen that the voltage across the biasing potentiometer *P*6 will drop instantly to zero, and the current-limit tube 5 will start conducting full current as soon as *CR* contacts start to move. The plate current of the master control tube 3 thus will rise to its full value corresponding to zero grid voltage, and, consequently, the rectifier tubes 1 and 2 will stop conducting, so that only a slight spark can appear on the main armature contacts, and no arcing will occur. It may be interesting to note that, owing to such a circuit arrangement, the main armature contacts need not be designed to handle the actual rupturing of the armature current, and this will often permit the use of contactors of smaller size to handle the armature circuit.

Conclusions

The control circuits described in this paper have included only basic elements needed for full understanding of the performance of the drive. The addition of a few more elements will secure a number of additional functions and features. A complete commercial control as shown in Figure 13 will usually include means for motor reversal, dynamic braking, antiplugging, and extension of speed range above the base speed by field weakening.

A control like that is a package unit complete in itself, and it incorporates all necessary devices to provide adequate protection against sustained overloads, low voltage, and short circuits.

The drive is normally controlled from a remote pushbutton station which contains forward, reverse, and stop pushbuttons and speed adjuster.

The important characteristics and features of the modern rectifier drive are as follows:

1. Wide and stepless speed range.
2. Automatic regulation to a preset speed.
3. Full torque at low speeds.
4. Smooth shockless current-limit acceleration with full control of starting torque.
5. Automatically controlled reversal of the motor.
6. Several preset forward and reverse speeds, if desired.
7. Elimination of all rotating parts except the motor.
8. Convenient remote mounting of small control stations.
9. Operation from a conventional a-c line.

References

1. Theory of Rectifier D-C Motor Drive, E. H. Vedder, K. P. Puchlowski. AIEE Transactions, volume 62, 1943, pages 863–70.

2. Thyratron Control of D-C Motors, G. W. Garman. AIEE Transactions, volume 57, 1938, June section, pages 335–42.

3. Electronic Speed Control of Motors, E. F. W. Alexanderson, M. A. Edwards, C. H. Willis. AIEE Transactions, volume 57, 1938, June section, pages 343–52.

Radio-Noise Elimination in All-Metal Aircraft

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I. Introduction

RADIO noise is the result of interaction among four components of the airplane, namely, electrical equipment, the radio receiver, the electric wiring, and the antennas. These components are produced by three different groups of manufacturers who have until recently made no effort to co-ordinate with each other on the radio-noise problem, the solution of which requires that these components be considered conjointly and that each party accept his part of the total responsibility. Until recently, this joint responsibility was not recognized, but each party was held to a rigid set of specifications which restricted them all to a very narrow line of behavior and made them responsible only for the meeting of some specific requirements. As a consequence, the various manufacturers considered their obligation as fulfilled when these specific requirements were met, whether the radio system was noise-free or not.

With the advent of wartime demands for increased production rates, there is a tremendous added incentive to adopt any feasible method which promises to expedite construction and save material. The increased use of special radio equipment requires that this problem be approached from an engineering standpoint in order that the most economical use may be made of available radio-noise-elimination methods. An intense eleventh-hour effort must be made to apply these radio-noise-elimination methods, so that obsolete practices can be replaced by modern-design practices, and the designer will accept the responsibility for them. This may be done by the application of well-known principles of circuit coupling, shielding, and filtering to aircraft electrical systems.

Shielding as a means of radio-noise reduction was first applied to ignition systems which were found to be intense sources of radio noise.[1–8] The exacting requirements of this type of shielding led designers to believe that these methods must be applied meticulously throughout the airplane. This erroneous assumption led to the specification of electrical systems which were entirely enclosed in metallic shielding, with extreme and unnecessary requirements of electrical conductivity of the shielding elements. (Junction boxes, conduit, and bonding connectors.)

It is noteworthy that the utilization of such measures has not resulted in radio-noise-free systems. The main result has been to make these burdensome requirements absolutely prohibitive under wartime conditions. Consequently, this has forced a reconsideration of these requirements, particularly regarding the installation of conduit for circuit shielding.

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The elimination of the majority of conduit from military aircraft is resulting not only in economy of labor and material but also in minimizing vulnerability to gunfire and facilitating repairs in flight and in the field.

The weight saving is also considerable, as the removal of conduit brings with it the removal of junction boxes and plugs and in many cases reduces the need for wiring devices, such as terminal strips, connectors, and so forth, as circuits need not make as many junctions as before.

By the application of sound engineering principles it has been found that many of the restrictive measures once considered essential may be eliminated. Practically all conduit, with the exception of the conduit on the ignition system and some radio wiring, may be eliminated. This of course requires that the system of rigid detail specifications be abandoned and the emphasis put upon performance requirements of the completed airplane.

The recent change in policy of the Army and Navy specifications is a large step in the proper direction. This step will tend to exert the necessary pressure on the "three parties" previously noted to cause whatever co-ordination is necessary between them to establish the proper responsibility of each.

II. Nature of Radio Noise

There are numerous sources of radio noise, but as far as radio noise in aircraft is concerned they may be placed in two general categories. One is produced by atmospheric phenomena, and the other is produced by aircraft electrical equipment.

The radio noise produced by atmospheric phenomena is often of great intensity and manifests itself in the receiver output in the form of intermittent crashes from lightning discharges or a more or less continuous noise which ranges from a harsh crackling to a high-pitched rather musical tone.

Investigations of this type of noise have disclosed that the electrical conductivity of an airplane flying into charged precipitation particles causes changing field gradients such that currents flow to or from the airplane in order to neutralize this condition. Discharge currents from the airplane produce localized noise fields which couple to the antenna circuit.

Some attempt has been made to reduce the production of localized noise fields by the use of a trailing conductor attached to the tail of the airplane in order to dissipate the accumulated charges in a region where their field of influence does not reach the antennas. The success of these static dischargers depends to some degree upon the existing meteorological conditions being more effective in dry snow and sleet than in warm rain. The device is, at present, only one of many devices being tried as a solution to this problem.

Since we know that shielded loop antennas provide considerable relief from precipitation, static interference is due to the fact that the electric-field component of the total noise-influence field is large in comparison to its magnetic component where precipitation static effects are concerned. The loop is shielded against the electric-field component but is receptive to the magnetic component of both the noise and signal field. An over-all gain in signal-to-noise ratio results, as the electric- and magnetic-field components of the signal are equal at distances greater than several wave lengths away from the signal source.

The intensity of the aforementioned type of radio noise is extremely high, and only a partial solution to this problem has been obtained thus far. In the normal case this type of radio noise will easily override radio noise from any other source. This problem is well covered by other observers,[9,10] and is not under further consideration in this writing.

Under conditions and in regions where none of the disturbances already noted are in existence there still remains a certain background level of noise field intensity generated by cosmic disturbances. This noise field varies between 2 and 20 microvolts per meter, in the 0.2 to 50 megacycle range.[11,12]

The preceding paragraph represents the limiting background level for radio reception and thus provides a starting point in determining the degree to which noise-reduction measures must be carried out. This analysis is concerned chiefly with the determination and elimination of radio-noise coupling produced by electrical equipment associated with the airplane itself and with the elimination of as many coupling paths as possible by proper design of radio equipment and radio installations.[11,12]

The correction of radio-noise problems at the Douglas Aircraft Company between 1938 and 1940 was carried out mostly on a trouble-shooting and fix-it basis. The inadequacy of this approach led to the realization that the problem should be handled by using proper engineering methods in the first place so that conditions leading to the coupling of radio-influence sources to radio equipment could be avoided. Extensive bonding and shielding could then be reduced to the amount actually required to ensure a noise-free installation.

III. General Considerations

In order to provide a proper basis for attacking the problem it was necessary to institute a program of preliminary research work to indicate the scope of the problem and the most economical avenues of attack.

The objectives of this program were to determine all paths through which radio influences produced by aircraft electrical equipment were being coupled to the receiver in a manner to produce radio-noise output and to determine the means available to eliminate these coupling paths. The major noise coupling paths were found to be:

1. Induction coupling between unshielded receiver lead-in conductors and nearby electrical equipment (and wiring in conduit in cases where the conduit was not bonded effectively to structure). It must be noted that open wiring was not at this time being considered for installation in an airplane.
2. Conduction coupling through receiver power-supply leads, or through other conductors entering the receiver.
3. Various conbinations of induction and conduction coupling, such as conduction coupling to the transmitter.

At this point is was recognized that conductive noise-influence coupling was a problem separate from that of shielding, and that many former cases of radio noise actually caused by conductive coupling were blamed on imperfect shielding and bonding. The result was that more extensive care was given to shielding and bonding at a time when the accent should have been placed on a determination of the actual trouble and a proper analysis of the whole problem. The development of receiver filtering to eliminate conductive coupling as it was known to the automobile field was apparently being ignored by makers of aircraft receiving equipment.

The methods available for aircraft-radio-noise control were found to be no different than those applied to other fields, but the application of these methods to a specific device, the all-metal airplane, appeared to be capable of considerable modification. The following salient observations indicate the manner in which shielding, bonding, and filtering are related to the all-metal airplane:

1. The all-metal structure provides excellent shielding between the internally produced noise-influence sources and the antennas.

2. Compartmentation of the airplane provides further shielding between certain areas.

3. Inherent structure of the airplane provides incidental shielding from one point to another within a compartment.

4. The ground plane effect of both primary and secondary structure provides shielding whose effectiveness depends upon its geometry.

5. The effects of openings in the fuselage, or openings between compartments, in decreasing the effectiveness of such shielding depend also upon the geometry of the particular case.

These basic observations led to an enlargement of the original research program to include a consideration of open wiring, as there was definite evidence that noise-influence coupling was already restricted to localized points by reason of the inherent shielding ability of the airplane structure.

A detailed consideration of the function and application of shielding is given later.

6. Bonding from the standpoint of radio-noise reduction is entirely subservient to shielding and is not a means in itself. The use of bonding connectors, jumpers, and so forth, is simply a means of making shielding electrically effective. It is to be especially emphasized at this point that the ohmic resistance alone of a bonding connector is no measure of its effectiveness. Its impedance at the radio frequency under concern is usually of much greater importance, as a very short connector may have a reactive impedance of several ohms at 20 megacycles.

Bonding, to say the least, has been overdone to a fantastic degree, resulting in the accent on radio-noise reduction being placed on the bonds and bonding connectors themselves rather than on their function.

7. Were all shielding and bonding accomplished to the necessary degree to eliminate induction coupling to receiver lead-in conductors and were the necessary filtering incorporated in all receiver circuits to eliminate conduction coupling, there would be no necessity for the installation of separate filter units in the airplane. Since this cannot be accomplished overnight, the radio-interference filter remains a handy device to aid in reducing radio-influence-voltage (RIV) coupling where this results in actual receiver-noise output. The most legitimate use of such filters is in eliminating conduction coupling to receivers when it occurs to a degree resulting in noise output. In this application, the filter is placed as near the affected (receiving) equipment as possible, as it is most effective at this point and its weight is least.

It is obvious that as soon as the necessary filtering units are incorporated in the equipment adversely effected by RIV that the aircraft manufacturers will no longer be concerned with installing these units separately. If induction RIV coupling to lead-in conductors occurs, the choices of correction lie among shielding the lead-in, shielding the offending wiring (or device), or installing a filter between the point of coupling and the RIV source. If shielding is done properly, the installation is free of induction coupling no matter what changes occur to the source of RIV. If filters are used instead, they become heavier as the RIV source is approached and each change in the electrical equipment may dictate new filter requirements.

The preliminary analysis has shown that the design of the completed radio system must take into accord all cofunctional parts of the system and that acceptance of the system must be based on the performance of the radio equipment as installed in the airplane and not on measurements made on the separate parts of the system. Such performance requirements must be augmented perforce by certain specific requirements where these definitely are known to be required. Such requirements must, however, be based on actual proof of their need rather than on the basis that their inclusion will provide a safeguard for conditions that may arise in the future. This attitude to date has been generally accepted with respect to shielding and bonding but has not been carried out with respect to filtering because:

1. Early experience with open-wiring systems has led to some transitional difficulties which were cured temporarily by the application of filters. This has led to an abnormally extensive use of filtering in order to cover up a poor job of RIV decoupling that should have been accomplished by proper placement and/or shielding of lead-in conductors. The outcome of this has been the arbitrary establishment of certain RIV limits to aircraft electric circuits and connected equipment units. The principles that apply to shielding and bonding also apply to filtering, and it is important that arbitrary limits or requirements of RIV, design requirements, and so forth, be stipulated only where these have been shown necessary by test under operating conditions.

2. Measurements of RIV on installed wiring or equipment are no measure of receiver-noise output unless a specific test is made to determine such correlation or coupling factors. Such a test must be made under the actual conditions of installation in the airplanes as any deviation from the actual conditions of installation in the airplanes will usually invalidate results entirely. It is hence a fallacy to ascribe other than academic significance to measurements made at any point other than the receiver output, unless the coupling factors to various noise sources have been determined. It is thus meaningless to stipulate any general limits of RIV, since acceptable limits vary tremendously from one installation to another.

IV. Shielding Design Considerations

Screening of magnetic and electric induction fields is attained by the use of shielding elements. In the practical case, these elements may take the form of an already existing metal fuselage, compartments or partitions therein, or portions of the structure so situated that their inherent screening effect can be utilized. Where necessary, shields or enclosures are constructed additionally.

1. ALL-METAL STRUCTURE

The all-metal structure of present military aircraft has been found to constitute a very excellent shield capable of screening internally generated noise fields from the antenna. That this shield is not complete structurally does not alter the basic premise and only means that the effect of openings, such as windows and plastic glass enclosures, gun turrets, and so forth, must be considered in the layout and placement of wiring. Such openings represent the possibility of induction coupling between antennas and internal-noise fields; however, tests show that this coupling can generally be neglected unless either the antenna or the noise-field source (usually a wiring bundle) is very close to the opening. It is usually a simple matter to route the wire bundle a satisfactory distance away from such openings or to shield it in some other manner. In the extreme case it may be necessary to utilize a piece of metal conduit to shield the conductors for a short distance if a test shows this to be necessary. Attenuation because of aircraft structure is usually greater than 60 decibels.

2. ELECTRICAL-EQUIPMENT NOISE-INFLUENCE FIELDS

The induction fields associated with aircraft electrical equipment, such as generators, dynamotors, and so forth, are at their greatest intensity directly at the device; however, this intensity drops very rapidly with distance, so that it may be negligible when measured at a distance of a few feet. When parts of the aircraft structure intervene between a noise-field source and the point of observation, a further drop in field strength is noted because of the screening effect of the intervening structure. The attenuation obtained depends upon the geometry of the source circuit, the detection circuit, and that of the intervening shield.

3. SHIELDING OF RECEIVER LEAD-IN

If the lead-in conductor of every receiver could be shielded totally from the receiver to the antenna insulator, there would be no problem with regard to noise-influence fields coupling to this conductor. It is, however, difficult to shield completely the lead-in conductors of aircraft receivers unless these are very short.

A compromise must therefore be made between what is desired and what may be obtained in practice.

4. SHIELDING OF WIRING

Since the shielding of electrical wiring, other than antenna lead-in conductors, can be done without regard to the capacitance of the shield, it is possible that portions of the wiring coming into close inductive relationship with the lead-in can be more easily shielded than the lead-in. In this case, the shielding needs to extend only to the point at which the wiring is again shielded inherently by the structure. It is, of course, poor design to place dynamotors and other equipment known to produce very intense noise fields, close to the receiver lead-ins, thus pointedly aggravating the shielding problem.

There is no substitute for experience in the matter of determining just how far design can ensure a satisfactory installation when noise fields very close to the lead-in are concerned. The present analysis has greatly reduced the areas under consideration so that the cost of adding a little more protection than is actually necessary as a safety factor is small compared to treating the entire airplane in this manner.

5. PLACEMENT OF RADIO EQUIPMENT

The considerations of noise coupling to the lead-in immediately suggest the placement of the receiver, transmitter, and antenna switching relay very near the antenna lead-through insulator so that only those noise field sources within a few feet of the receiver lead-in need be considered.

This narrows the investigation to a small area at which radio-noise-control methods can then be concentrated.

Such placement will result in the following desirable advantages:

(*a*). That portion of the lead-in (from the insulator to the relay) which is common to both receiver and transmitter can usually be left unshielded entirely.

If this portion must still be several feet in length and *must* also be within a few feet of some unavoidable noise source, such as parallel wiring, it is possible to shield this portion, meeting the requirements of spark-over insulation, and low capacity, without great difficulty.

(*b*). Portions of the lead-in between the antenna switching relay and the receiver will require shielding only if they are in very close proximity to noise-field sources.

The shorter these portions are made the more nearly is the ideal case approached in which even those noise-influence fields in the same compartment with the receiver and lead-in are of no concern, as the coupling factor becomes negligible.

(*c*). The placement of the receiver chassis close to the antenna lead-through insulator reduces the radio-frequency impedance of the structure return of the antenna circuit of the receiver. This is exceptionally important as radio-influence current (RIC) enters the receiver through various external circuits, and while these currents may not affect the receiver directly, they flow from the receiver chassis ground to structure through this structure return impedance. This circuit is seen to be in series with the antenna circuit of the receiver and thus noise voltages will be coupled conductively into the antenna circuit.

The focus of attention has now been directed to the importance of equipment placement in order to reduce the area within which protective measures need be applied. When it has become evident to the designer that the total problem becomes much simplified by keeping RIV sources away from the lead-ins, or vice versa, and steps are taken early in the design stage to achieve this, the installation becomes so simple that visual inspection together with some experience will be all that is required to lay out the radio system with a good expectation of success. It is prudent in all cases to allow for a certain amount of auxiliary shielding (in critical or not easily appraised circumstances) that can be dispensed with if preliminary tests show this feasible.

V. Functional Reasons for Bonding

The reasons for bonding all-metal aircraft can be summed up as follows:

1. To obtain a low-resistance current path for electrical equipment utilizing the airplane structure as a return circuit. Bonding connectors used as a current return must meet the same requirements as to conductivity, contact resistance, impedance, and current-carrying capacity as is required of the circuit with which they are associated.

In this connection it should be noted that it is impossible to rivet and bolt together the metal parts making up the airplane without obtaining inherently excellent electrical conductivity. The ohmic resistance of a *DC*-3 airplane measured from nose to tip of tail was found to be 0.0002 ohm. From this it can be assumed safely that, except for the very largest conductors used, once a good connection is made to primary structure no consideration need be given the resistance of the structure itself between any two points on the airplane. However, in the event connection is made to secondary structure, ample consideration must be given to the resistance between this secondary structure and the primary structure of the airplane. This is of special importance in specific cases such as in bonding radio receivers or in the calculation of generator negative-lead resistance. The fallacy of adding an equal resistance to the conductor resistance for calculation of total circuit drops is indicated. This has been borne out by tests on both old and new *DC*-3 airplanes with regard to the determination of all voltage drops along the starter circuit. It was found that the ohmic resistance of the copper conductor was far greater than that of the structure in every case.

2. To minimize lightning damage. Present literature on lightning strokes to aircraft reveals that apparently the aircraft is never required to carry more than a thousand ampere-seconds during a single stroke. The best approximation obtained from data taken from direct lightning damage to aircraft indicates that if major external surfaces of the airplane are so bonded together that 1,000 amperes can be conducted between any two points for one second, ample protection against damage is assured.

The chance that the airplane will ever carry a cloud-to-ground stroke is remote. The strokes are usually cloud to cloud and are triggered by the airplane flying into an area of intense electrostatic stress.

The installation of bonding connectors capable of carrying 1,000 amperes for one second, across aileron hinges, flap hinges, and large control-surface hinges apparently offers adequate protection against welding of these hinges by reason of lightning current passing through these points. Such bonding connectors must be very short in order to possess the least possible radio-frequency impedance. Bonding connectors may be omitted from small surfaces, such as trim tabs, unless they become of larger size than they are at present. A $^{13}/_{64}$-inch copper braid has been found capable of meeting the required rating.

3. To insure the effectiveness of shielding, the d-c ohmic resistance to structure for any shield is of little consequence compared to the radio-frequency impedance to structure. The radio-frequency impedance of a bonding connector may be several thousand times the d-c resistance per unit length. Hence, in bonding such shields, emphasis is placed on obtaining the shortest possible bonding connector and not on obtaining any arbitrary value of d-c resistance to structure.

As this shield must be effective in screening both the electric and magnetic fields, it is necessary that the shield form a completed circuit. This usually calls for bonding to structure at both ends of the shield or conduit, as the case may be.

4. To provide a means for grounding the airplane when it lands. Upon landing, an aircraft must discharge to ground whatever electric charge has been assumed by the aircraft during flight. The use of conducting rubber in the landing-wheel tires will serve to conduct this charge to ground provided the runway surface is made of partially conducting material. The use of grounding wire from the gasoline truck to the airplane is recommended as a safety precaution at

all times to equalize any potential difference between them.

VI. Filtering

Correctly applied shielding and bonding will result in the elimination of induction coupling between RIV sources and receiver-antenna circuits including the lead-in conductors. Such isolation may not account for all the RIV suppression necessary, as conduction coupling may occur by means of direct transmission through the conductors interconnecting the radio equipment with the RIV source. When such noise coupling occurs to a degree causing radio noise, the insertion of filter units may become necessary.

A radio-influence filter is defined as a device incorporating shunt and series circuit elements so connected as to allow the passage of desired currents through the filter and to prevent the flow of undesired noise-influence currents. Such a filter is no different in principle than a power-supply filter, which allows the passage of direct current and prevents the flow of alternating current to the load, except that the radio-noise filter must discriminate against a much wider band of frequencies.

It is a principal contention that filters should be used only where they perform a specific function with respect to specific equipment. Filters of this type are easily designed to be mounted integrally with radio equipment, as they are usually very small because of the low current-carrying requirements.

Properly designed receiving equipment will have filtering incorporated such that proper operation will result when connected to the aircraft electrical system without further attention to this matter. When this is not the case, a separately packaged filter unit must be applied at some point between the receiving equipment and RIV source. Experience with aircraft-radio-noise problems has shown that filter design together with its proper incorporation into the circuit is a highly technical matter, dictated by the specific installation, and cannot be handled properly on a general basis. The same considerations make it extremely difficult to stipulate a meaningful limit of RIV allowable as either an electrical-equipment specification or as a limit to be applied to aircraft electrical circuits.

The design of a radio-noise filter is very critical, as brute force lumping of capacitors and inductors will not necessarily result in the desired characteristics.[18] A simple filter consisting of a series inductor and shunt capacitor designed to cut off at ten kilocycles will be practically useless at ten megacycles, as the distributed capacity of the inductor will make it resonant at some frequency below ten megacycles so that the inductor will actually be capacitative above the resonant frequency. The capacitor resonates with its inherent series inductance at a certain frequency and becomes inductive above this frequency, thus losing its value as a shunt reactance. A filter unit to provide a given attenuation over a frequency range of 200 kilocycles to 50 megacycles would hence require several stages, each stage being designed for a certain band. Fortunately, the noise-influence spectrum of almost all aircraft electrical equipment, *with the exception of the ignition system*, shows a great diminution of intensity with increased frequency so that at two megacycles very little radio-noise energy is left. This fortunately simplifies the filter-design problem with respect to the high-frequency end of the band. The series element must possess a large amount of inductive reactance in order to provide the desired attenuation at low frequencies as the RIV increases tremendously at the low frequencies as is to be noted in Figure 1. This does not become too troublesome from the weight standpoint for filters required to pass less than 50 amperes. If the filter is used as a source filter for heavy-current electrical equipment, the series element not only must carry the full-load current of the device but must meet a maximum-voltage-drop requirement (0.2 volt) and thus becomes very heavy. The current rating of aircraft generators has reached several hundred amperes and is still rising. The weight cost of any filters used must be multiplied by a factor which depends upon the amount of structure chargeable to the incorporation of one pound of equipment and the weight of gasoline required to carry it. This factor may increase the weight cost of a filter several times.

The RIV generated by thyratron rectifiers as used in some types of voltage regulators ranges up to 4,000,000 microvolts (four volts). This value is greatly in excess of that normally found in the worst electrical equipment. The design of a source filter for such a circuit presents almost insurmountable problems because of the limitation placed on capacitor size in the rectifier output circuit. The resulting required inductor size to attain even a moderate decrease in RIV is prohibitive for aircraft. In one case of the use of such a voltage regulator on a 400-cycle power system, this extreme RIV source was found to cause no trouble in the completed installation. This was due to the attenuation of the RIV resulting from the filtering action of the supply conductors, battery charger, and connected battery. No RIV of a disturbing level reached the communication receiver. This receiver was later found to be productive of radio noise with less than 500 microvolts on its supply leads, hence the original RIV (four volts) must have undergone roughly an 8,000 to 1 voltage diminution or 78 decibels in power.

Dynamotors are of low current rating in comparison to generators or other heavy-current equipment, and hence this filter is not representative of the larger ones that would be required for such equipment. Each filter section contains at least one series element and two shunt elements with their attendant connections, yielding several extra items capable of giving trouble. The addition of each filter represents an extra hazard to electrical equipment resulting from the possibility of short or open circuits.

The allowable temperature rise of some equipment is so high that no capacitor can be placed in direct contact with the motor frame without the risk of breakdown and resultant short-circuiting of the motor. This means that the economy of space attained by mounting the filter integrally with the motor or device base is lost, and the filter must be mounted separately.

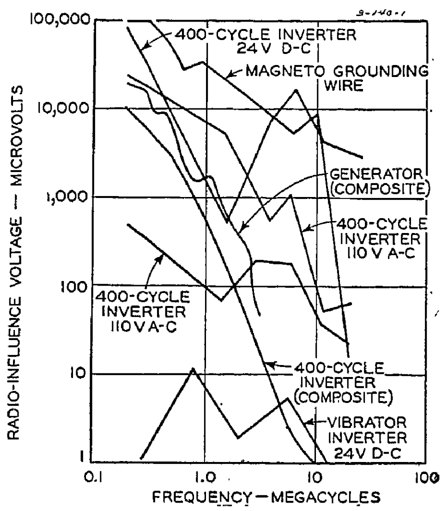
The necessity for mounting the filter assembly separately arises also because of an ultrahigh premium on space in the vicinity of engine-mounted equipment and in other congested locations. Only in certain cases can source filters be incorporated neatly in the equipment itself. The space occupied by certain externally mounted dynamotor filters is greater than that of the dynamotor.

Experience has shown that considerable conservation of space and reduction of weight of filters consisting of coils and capacitors may be realized by the proper use of simple noninductively wound capacitors. For example, a certain filter weighing two pounds and occupying a space of 36 cubic inches may have an average attenuation of 50 decibels over the frequency range of 0.2 to 20 megacycles. A simple capacitor may have an average attenuation of 30 decibels over the same frequency range, will occupy a space of only eight cubic inches, and will weigh only a few ounces.

If use is made of capacitors as RIV filters, considerable judgment must be exercised in their electrical location. Improper electrical location of capacitors will result in an increase rather than a decrease in coupling to a RIV source. This arises from the fact that connection of a capacitor to conductors carrying RIC will increase the flow of RIC between the capacitor and the source. If these conductors are in the vicinity of the antenna lead-in or other conductors that can couple RIV to the receiver, the increased flow of RIC may result in an increase of receiver-noise output.

Since any capacitor will resonate at a certain frequency and will become an inductive reactance above this frequency, the length of the connecting leads to the capacitor should be held to an absolute minimum to extend its effectiveness to as high a frequency as possible.

It must be noted that some sources of RIV cannot be adequately filtered by means of simple capacitors. A magneto grounding conductor, for example, may not be shunted by a capacitor without affecting the operation of the magneto.



Figur 1. Typical frequency spectrum of RIV sources

A capacitor of this type is employed advantageously at the terminals of the booster coil.

A summary of major points concerning filter application follows:

1. Filtering should be designed as an integral part of all receiving equipment or other equipment being affected by RIV on supply and control conductors to such equipment. This avoids forcing the use of much larger, heavier, and more numerous filters elsewhere in the electrical system.

2. Where RIV induction coupling occurs to the receiver antenna circuit, isolation methods employing shielding of electrical circuits or of the exposed antenna circuit should be employed in preference to filtering of the electrical system or electrical equipment. Once such isolation is achieved it will be equally effective with future changes of electrical equipment provided such shielding has not been violated inadvertently.

3. Dependence on system or source filtering means each source must be equipped with a filter to reduce its RIV to a given limiting value. It has to date been impossible to ascribe a limiting value that is meaningful to a practical extent because of variations from one aircraft electrical installation to another.

The enforcement of such an arbitrary limit would be troublesome because of difficulties of measurement. This point is covered more fully under "Radio Measurements."

4. Simple capacitors may be used as RIV filters where such use will not result in malfunctioning of the equipment being filtered or cause other difficulties attributable to the increase of RIC caused by the admittance of the capacitor.

VII. Radio-Noise-Influence Producing Equipment

Ignition circuits, motors, inverters, generators, and their associated voltage regulators are the main body of equipment items which constitute the sources of RIV and RIC with which we are concerned. These items usually produce also a noise-influence magnetic field surrounding the equipment item itself. This field diminishes rapidly with distance and usually constitutes a hazard only when placed close to exposed receiver lead-in conductors. The noise-influence electric field is notably absent near these devices provided their cases are suitably grounded to structure. The use of an electric field probe to measure the possible radio-noise-influence effect of such an item is not practical, as the probe antenna does not simulate the loop configuration of a receiver lead-in conductor.

1. Audio-Frequency Components

It is generally found that the random-noise energy produced by these devices has its highest energy content at audio frequencies (the actual spectrum varies depending upon the function of the device) with rapidly decreasing energy at the harmonic frequencies extending into the radio spectrum. The high audio amplitude of commutator ripple requires an audio-noise decoupling filter in the microphone polarizing current circuit of the interphone amplifier. The background noise noted in other audio circuits, such as the circuits of radio receivers, also contains this ripple. The gain of these amplifiers is usually so low that this does not cause a bothersome noise level.

The shunting effect of the aircraft storage battery is of such magnitude in maintaining a low audio background noise level that when a battery cart is substituted for the airplane battery a considerable increase in audio background noise may be noted. This is due to the additional d-c resistance of the leads to the battery cart, acting to reduce the normal shunting effect of the battery. Consequently, any noise test made with the battery cart connected or auxiliary battery supply line connected in any manner differing from the normal system installation, should take this effect into account. The battery also acts as a filter capacitor at low radio frequencies, and the effect of extra battery cart lead length is much greater than at audio frequencies. This effect increases with frequency as the radio-frequency impedance of the leads becomes a larger factor than the d-c resistance.

2. Radio-Frequency Components

The radio-frequency spectrum of concern in this analysis begins at 180 kilocycles and ends at 30 megacycles, except where noted otherwise, for instance ignition circuits. The RIV produced by a 400-cycle instrument inverter is very low at 180 kilocycles in comparison with the audio-noise frequencies. The RIV is, however, high at 180 kilocycles compared with its value at 15 megacycles, as noted in Figure 1. The audio-frequency noise-influence voltage is of a magnitude of several volts, while the RIV at 180 kilocycles is rarely above 100,000 microvolts (0.1 volt) at the terminals of the dynamotor.

Representative curves, Figure 1, of RIV as a function of frequency have the following characteristics in common:

(*a*). Highest output at the lowest frequency.

(*b*). Output drops as frequency increases but may go through several resonant peaks before finally dropping to insignificant values.

(*c*). Output drops to negligible value beyond 15 megacycles.

Note that these observations also hold for measurements made on ignition grounding circuits in spite of the fact that secondary impulse currents are still of considerable amplitude at 500 megacycles. This apparently is due to the fact that little or no coupling exists between the ignition secondary circuit and the magneto primary circuit at these frequencies. Ultrahigh frequency coupling may still by-pass the magneto through other paths. The secondary ignition circuit of an airplane engine is a prolific source of radio-noise-influence fields which couple directly to the aircraft antennas. These fields are of considerable strength

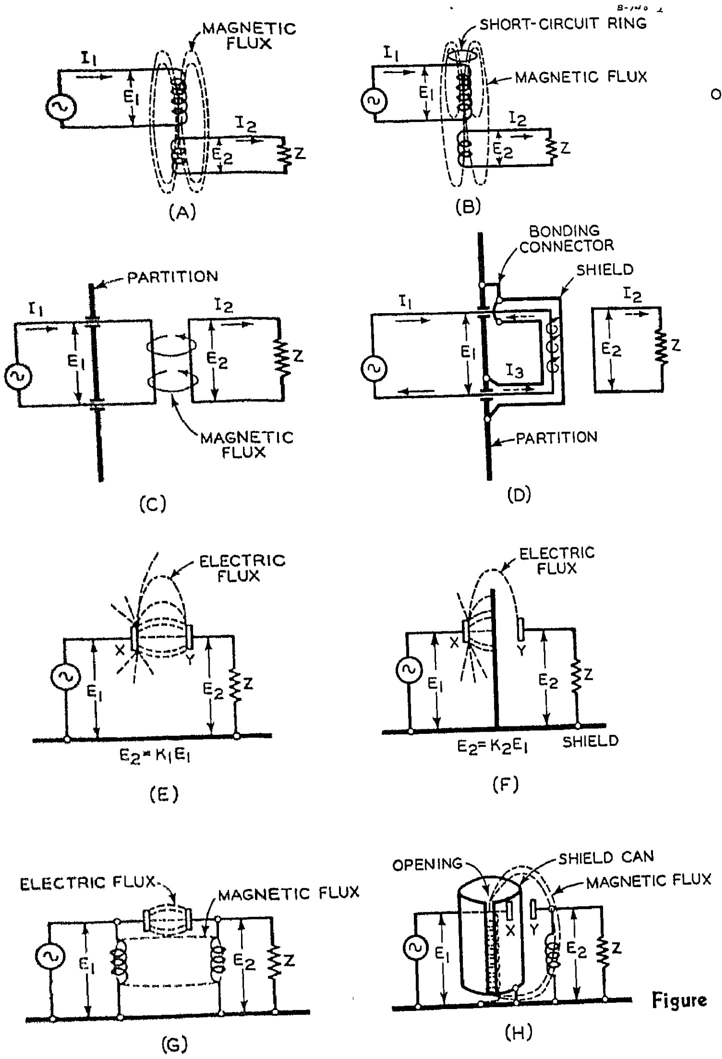


Figure 2 (left). RIV coupling diagrams

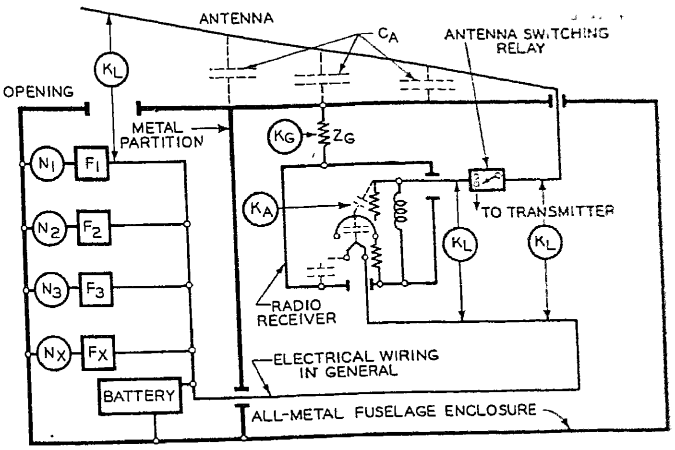


Figure 3. Aircraft radio-noise-coupling diagram

up to 500 megacycles. The problem of shielding this circuit in order to eliminate this radiation is one requiring detailed consideration. This subject will be considered further in a subsequent writing.

VIII. Fundamentals of Noise Coupling Circuits

The transference or transmission of electric energy involves coupling between two or more circuits. This is accomplished through the medium of impedances common to the coupled circuits. The common impedance may consist of resistance, capacitance, inductance, or any combination of these.

It is necessary to distinguish between induction and conduction coupling. Induction coupling takes place through the medium of the magnetic and electric fields and is referred to as inductive and capacitative coupling, respectively. Conduction coupling takes place where a noise-influence current flows through an impedance such as a bonding connector which happens to be common with another circuit, such as the antenna input circuit of a receiver. Such coupling occurs between RIV sources and receivers through the conductors connecting them and thus is not affected by shielding of these conductors.

In the circuits with which we are concerned, there is often no clear definition as to the type of coupling which is present, and frequently all are present at the same time. For analysis let us investigate each type separately. Before entering into the analysis, the question of terminology must be considered to avoid confusion.

In the discussion to follow the multiplier K is used to denote a coupling ratio.* For example, if a voltage E_1 is impressed on the primary terminals of a transformer, then the transformer secondary voltage will be: E_2-KE_1.

K, then, may be either larger or smaller than unity in the case of a transformer and in any event will be a complicated expression. The voltage appearing at the output terminals of a network may be obtained by multiplying the voltage impressed on the input terminals by K.

Assume a certain electrical network has a coupling ratio K_1, and in some manner the network is changed so that the coupling ratio is now K_2. Let us say further that this change is such that K_2 is smaller than K_1. The attenuation attributable to the change in the electrical network may then be expressed as: A = Attenuation = K_1/K_2. This may be expressed either as a simple numerical ratio or in decibels, in which case $DB = 10 \log K_1/K_2$.

The "coupling ratio" is also conveniently used to express the relationship of two different quantities, in which case their measurement units and conditions of measurement should be clearly indicated. For example: the coupling ratio between the liaison antenna and the ignition system is 98 at 1.0 megacycles. The ratio 98 expresses the microvolts per meter measured with a Ferris Model 32A radio noise meter and 41-inch probe antenna at a test location three feet forward of the propeller hub, to the voltage measured with the same meter at the antenna with the antenna terminated only in the radio noise meter.

The convenience of the use of such a factor lies in the elimination of the need for the inclusion of many academic factors such as the effective height of the antenna, field pattern considerations, and so forth, which are difficult of determination. Such factors mean little when the over-all effect is all that is needed to evaluate a particular case.

1. Inductive Coupling Through the Medium of the Magnetic Field

Referring to Figure 2A, a generator impresses a voltage E_1 upon the primary side of a coupled circuit. The magnitude of the voltage E_2 induced in the secondary side of this circuit will depend upon the mutual inductance of the coupled circuits and the current in the primary circuit. The effect of coupling the secondary side to the primary side is to couple an additional impedance into the primary side, the magnitude of which will be a function of the secondary series impedance and the mutual inductance of the circuit. Suppose that a single turn of wire, short-circuited on itself, is coupled to the primary coil as shown in Figure 2B. As this short-circuited turn is coupled more and more closely to the primary coil, the current in the primary coil will increase and the voltage induced in the secondary coil will decrease. The attenuation which results from coupling the short-circuited coil to the primary coil may then be written as $A=K_1/K_2$.

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* This use of K should not be confused with the symbol K as commonly used to denote the coefficient of coupling between two circuits. For instance: $K=M/L_1L_2$ where M = the mutual inductance between the two circuits, and L_1 and L_2 are the inductances of the two circuits, respectively.

The case with which we have been dealing is very common in coupled circuits, and the coupling ratio is often much larger than unity. However, in radio-noise work we are mostly concerned with instances where the coupling ratio is very small, as betwen two conductors at some distance from each other.

Referring to Figure 2C, a generator is feeding a wire which passes through a shield, for example, a wire passing through a metal partition in an airplane. Coupling exists between this primary wire and another wire some distance away, and a voltage is induced in this secondary conductor. Precisely the same relations hold as in the previous discussion. $E_2=K_1E_1$. Since the coupling between the two wires is of low order, K_1 will be much less than unity, and E_2 will be less than E_1.

Referring to Figure 2D, a metallic shield has been installed about the primary wire. At one end this shield is arranged to have good contact with the metal partition; at the other end the shield is connected to the partition by a bonding connector. It will be seen from this that there is a short-circuited turn again coupled to the primary circuit as in the first example. Consequently, a current I_3 will flow through the shielding and the partition comprising the closed turn, and there will be very little coupling to the original secondary wire. However, if the bonding connector is of high impedance, or poor contact is made, the closed turn will be incomplete, and there will be less coupling to the closed turn. As this process is continued until the closed turn becomes completely open, there will be less and less coupling to this turn, until finally the coupling is zero. When this occurs, the coupling to the secondary wire is again unaffected, and the shield about the primary wire might as well not be present, as far as magnetic field coupling is concerned.

In any event, the coupling ratios K_1 and K_2 vary as in the previous example, with the value of K_2 being lessened as the bonding connection is improved.

This shows clearly that a shield must be complete if inductive coupling is to be reduced. Furthermore, the resistance and reactance of the shield itself must be low, otherwise the efficiency of the shield is decreased.

2. Capacitive Coupling Through the Medium of the Electric Field

The general case is indicated in Figure 2E. A plate X is supplied by an alternating potential E_1 from the generator. An electric field will surround this plate and produce an electric-field gradient between the plate and the ground. If another plate is placed in this electric field, a part of this field will terminate on plate Y, and the potential difference between X and Y will be $E_2=K_1E_1$, where K_1 depends upon the capacitance between the two plates and the frequency of E_1. Now erect a shield between the two plates X and Y and ground this shield as in Figure 2F. The electric field will still surround plate X, but, depending upon the geometry of the shield, part of the electric field will now terminate on the shield instead of on plate Y. This has the same effect as moving plates X and Y some distance apart. K_2, the coupling factor between the two plates will become smaller, and E_2' will also be smaller than E_2, hence: $E_2'=K_2E_1$. Again, we may express the attenuation attributable to changed conditions, that is, the introduction of the shield, by the relation $A=K_1/K_2$ or attenuation in decibels$=10\log K_1/K_2$.

3. Coupling Through the Medium of Both the Magnetic and Electric Fields Simultaneously

Unfortunately, coupling is seldom entirely either inductive or capacitative. Usually, there is a combination of the two, in varying degree. It is interesting to note at this point that the frequency range involved in radio-noise measurements is so great that a circuit such as is diagrammed in Figure 2H will show a very large change in the value of K with frequency. In capacitative coupling, the maximum power transfer is a linear function of the frequency; in inductive coupling it is a function of the frequency cubed.[10] Therefore, as the frequency is increased, the effect of inductive coupling (magnetic field) becomes of far greater importance than capacitative coupling (electric field). Often, a certain type of shield may reduce both types of coupling, but in many cases a shield will serve only to reduce one type. Figure 2G illustrates a case where both types of coupling are present, and Figure 2H shows a shield installed so that there is no short-circuited turn. Here the electric field is almost entirely terminated on the shield, so capacitative coupling to plate Y is practically eliminated. The magnetic coupling remains relatively unaffected, because the shield does not form a short-circuited turn. An example of this form of shielding is the electrostatically shielded loop antenna.

4. Conduction Coupling Through the Medium of Common Impedances

Coupling occurs because an impedance is common to two circuits. In the cases just considered, this impedance was either inductive or capacitative, but in neither case was it some physical element of a circuit. Rather, the coupling was through the medium of the magnetic or electric fields.

In the case under discussion, coupling exists because an impedance, such as that between the receiver chassis and structure, is common to two separate circuits. Therefore, the flow of current in one of the circuits develops a voltage across this impedance and is thus introduced into the other circuit.

Referring to Figure 3 it will be noted that the impedance Z_0, formed by the receiver-bonding connector, is the major path to structure for any radio-noise-influence currents which enter the receiver by any path whatsoever. Signal currents entering the receiver via the antenna also return to structure through this path. This path being directly in series with receiver-antenna input circuit, any voltage developed across this path will be as effective in producing receiver audio output as an equal signal voltage developed in the antenna itself.

In addition to the common impedance formed by the receiver bonding connector, there are other coupling paths internal to the receiver. Among these are the impedances formed by the cathode to grid capacitance and the impedance of the cathode to ground.

Noise-influence currents that enter the receiver may divide and flow through the cathode-to-chassis or cathode-to-grid impedance before returning to structure. The RIV drops across each of these common impedances are amplified as they pass through the balance of the receiver to the audio output.

All of the radio-noise-influence currents that enter a receiver must return to structure through the impedance of the bonding connector. For this reason, when a RIV filter is used, it should be a choke-input rather than a capacitor-input configuration, because the current through the shunt element of the latter type will be greater, thus resulting in a greater RIV drop across the impedance of the receiver bonding connector.

Evaluation of the various common impedances is discussed under "Measurements of Radio Noise."

IX. Coupling of Radio-Noise-Influence Sources to the Radio Receiver

Many sources producing extreme amplitudes of RIV are so installed that this influence is never observed at the receiver. It must be clearly understood that a RIV source will be only of practical concern when it is coupled in some manner to one or more circuits of the radio receiver to a degree that the noise or other disturbance level occurring in the receiver output is greater than that permissible. Such coupling may occur to the antenna lead-in conductor of the receiver, to internal elements of the receiver (as by coupling between the cathode and the grid of a tube, where a difference of potential attributable to RIV modulates the cathode), and by means of RIC flowing through a common ground impedance. Well-designed radio receivers employ filter elements in all leads entering the receiver which are found to be critical with regard to RIV coupling to the low-level stages in the receiver. It is unfortunate that many aircraft receivers to date have not been given proper attention in this respect, thus complicating the aircraft-radio-noise problem tremendously and requiring the filtering job to be completed by the installer of the receiver. This has led to much unscientific use of filters in an attempt to find a panacea for all radio-noise ailments.

Radio noise output resulting from the induction coupling of wiring carrying RIV to unshielded lead-in conductors is a function of the following factors:

1. Amplitude of the radio-noise-influence voltage and current (RIV and RIC, respectively) on the offending wiring.
2. Degree of inductive coupling. RIV is the major factor in capacitative coupling, and RIC is the major factor in magnetic coupling.
3. Degree of conduction coupling.

Induction coupling exists to a troublesome degree only where the unshielded lead-in is in close physical proximity to unshielded wiring and is of no concern with respect to wiring in other parts of the airplane. In the region where coupling is of concern the degree of coupling is roughly proportional to the lengths of the lead-in and wiring exposed to mutual coupling, and to the amplitude of the RIV or RIC of the wiring in this region and is inversely proportional to varying powers of the distance between the lead-in and wiring.

A few general notes:

1. Most electrical equipment possessing commutating contacts or other current-switching elements produce RIV at their terminals which varies over large ranges of amplitude from one item to the next.
2. It is highly desirable that the design of electrical equipment be directed to the maximum possible reduction of such inherent radio influence.
3. Filtering elements can be applied to equipment to reduce the inherent RIV, but such procedure is wasteful unless it is determined first that the desired effect cannot be attained more economically elsewhere. In the usual case there are many more sources of RIV than there are units of equipment (receivers, and so forth) being affected. Source filtering is hence to be avoided where possible.
4. The cases in which correction *must* occur at the source are those in which the natural shielding of the all-metal structure is not effective. Such cases are represented by the engine ignition and electrically operated propellers of either all-metal or nonmetal airplanes. In nonmetal airplanes, the need for source filtering on or shielding of wiring obviously increases greatly.

X. Measurements of Radio-Noise-Influence Voltage, Current, and Fields

A considerable amount of time and effort has been expended in the last few years in the field of radio-noise measurements the largest portion of which has been done in the broadcast field.[13–16]

The term RIV has been used generically to represent voltage, current, or power. This generalization has obscured the fact that many coupling problems are functional with current rather than voltage. Magnetic field coupling is of this nature, as power transferred is a function of current, mutual inductance, and frequency cubed. Electric induction is a function of voltage, capacity between circuits, and frequency to the first power;[10] hence, it is seen that as frequency increases the coupling of radio-frequency power attributed to magnetic coupling (current loop) becomes definitely predominant as the frequency term is to the third power. It is thus desirable that the term radio-influence current (RIC) be used where measurements or effects functional with current are concerned.

Much progress has been made in the measurement of the absolute values of RIV of electrical equipment and circuits, but there has been a dearth of information concerning the evaluation of RIV in terms of actual receiver-noise output. An immense amount of aircraft-radio-noise research involving measurements of quantities having no relation to actual radio noise output has resulted from a lack of cognizance of the importance of the determination of such coupling factors.

A comparison of the broadcast-radio-noise problem to that of aircraft follows in order to note certain important differences in these two fields.

In aircraft, the placement of electrical and radio equipment, to take advantage of natural factors tending to reduce noise coupling from point to point, is under the control of the designer to a much greater degree than it is in the case of industrial-noise interference to broadcast listeners, where little such control can be exercised. The principle of filtering at the source applies very well in the broadcast field, and the application of noise filters to noise-influence producing equipment has gained well-earned headway.

This principle, however, does not apply to the airplane because the desired controls can be exercised to a very considerable degree.

The home broadcast receiver is coupled to RIV sources predominantly through the medium of its antenna and unshielded lead-in. Suppression of noise influence hence must occur at the source as there is no other way of decoupling the antenna from the source, as the antenna and the noise-influence source are both coupled to the identical free space.

In aircraft, the engine ignition system and electrically operated propellers are also in this category, and it is found that noise suppression must occur at the source by the employment of well-designed shielding.

All other electrical RIV sources and wiring are inherently shielded from the antennas by the all-metal fuselage. Internal lead-in conductors can be shielded against induction coupling to wiring, where necessary; hence, the problem is reduced to that of eliminating conduction coupling to the receiver. This is a filtering problem which is most easily solved by proper filtering at the receiver. Any attempt to attack this problem by source filtering leads to complications involving weight and specialized design of equipment to accommodate the filter.

In aircraft, radio-noise measurements must be made with the most compact equipment possible, while in other fields a truck full of equipment is no handicap.

The aforementioned points should be kept in mind as the measurement of radio noise in aircraft is being considered.

1. Radio-Frequency Measurements—General

The complete determination of all coupling factors involved in the coupling of RIV to receivers involves both a quantitative and qualitative analysis.

Quantitative analyses are those in which absolute amplitudes of RIV or RIC are measured at the terminals of electrical equipment or at various points in the electrical circuit, or in which absolute amplitudes of receiver-noise output are measured. From these measurements over-all coupling ratios can be determined without the necessity for determining intermediate coupling ratios from point to point. The coupling of a single source to the receiver usually involves many transformations of noise energy from one medium to another, each transformation experiencing a certain degree of attenuation or amplification as the case may be. The complete evaluation of these changes from point to point becomes extremely laborious if not impossible in all but the simplest cases. Fortunately, this is usually quite unnecessary. Intermediate coupling ratios may be of great academic value to the research engineer wishing to determine where and how coupling occurs from point to point but may be of no value to the engineer wishing to obtain only a performance test of the complete installation.

Qualitative analyses involve the determination of where and by what medium (induction or conduction) radio-noise coupling occurs. A qualitative analysis hence seeks to determine the conditions which cause coupling and to discover means for eliminating such coupling. Such measurements can be made from point to point with little consideration of absolute amplitudes of voltage, current, or field strength as the over-all (performance) quantitative measurement can be depended upon to determine the success of the methods employed. The academic usefulness of the qualitative analysis is in determining engineering-design practices which can be utilized by the designer and in trouble shooting when an unexpected case of radio-noise trouble occurs in the completed airplane.

2. Instruments for Measuring Radio Noise

The first measurements of radio noise were made by adapting an audio-power-level meter to measurement of the radio noise output of a conventional radio receiver. This was done by incorporating a time-delay response so that the meter would be quasi-peak reading in order to give a reading more nearly on the basis of the nuisance value, or masking value, of the noise voltage. It was soon found that measurements taken with such a meter connected to the output of a conventional radio receiver showed extreme variations when used with different receivers and that RIV measurements could be duplicated only if a standardized radio-frequency channel were provided. With the development of a standardized radio-frequency noise meter, radio-noise indications on the basis of nuisance value could be easily accomplished. This type of meter as exemplified by the Ferris Model 32*B* or *RCA* 312*A*, is suitable for the measurement of absolute amplitudes of RIV from point to point of the electrical system and to measure field strength of the electric component of radio-frequency and noise fields. It is not suitable to measure radio-frequency currents. The loop probe can be used to determine relative magnetic field strengths but will not indicate absolute values without specific calibration. The unit measured is the modulated microvolt in the case of noise-influence voltage measurements, and the modulated microvolt per meter, in the case of noise-influence field measurements. The following difficulties arise when this type of meter is used for aircraft-radio-noise measurements:

(*a*). Quantitative measurements made on the receiver input (antenna) circuit involve connecting the meter within a foot or two of the point to be measured. This is impossible in smaller airplanes during flight because of inaccessibility. Any attempt to use the meter remotely meets with trouble attributable to unsatisfactory grounding, noise pickup on the extended lead, and switching complications. The problems of switching such a meter from point to point during flight become very complicated and add a definite hazard, as the pilot must be able to regain control of the radio equipment at a moment's notice.

(*b*). Presently available equipment of this type is not rugged enough to stand continued use in aircraft (without excessive time being spent in adjustments and recalibration) and must be confined to the laboratory.

(*c*). The technique of measurement becomes so critical at two megacycles and above that none but those experienced in the technique of radio-frequency measurements can hope to obtain valid and consistent results.

(*d*). Battery packs required to operate these meters in aircraft bring the total weight to 45 pounds.

(*e*). The cost of the meter is high ($400), and its availability at present is practically nil.

To these difficulties is added the fact that all measurements made with a radio noise meter are only of academic value until they are evaluated in terms of receiver noise output. This type of meter hence has a high academic value in the laboratory and for field-strength measurements but a low practical value for production or prototype airplane-radio-performance tests.

Most of the difficulties arising from the use of a radio noise meter may be overcome by the use of an audio noise meter, in conjunction with the aircraft receiver under concern. The physical advantages of an audio noise meter may be noted as:

(*a*). Extreme simplicity of operation, and therefore suitable for production testing.

(*b*). No switching problem.

(*c*). Can be used as remotely as required.

(*d*). Can be used on the output of each receiver to obtain further simplicity of switching if desired.

(*e*). Portable and light in weight (eight pounds).

(*f*). Rugged.

(*g*). Inexpensive.

(*h*). Can be constructed from easily obtained materials.

(*i*). Reads directly the noise heard in the ear phones. No evaluation of coupling factors, receiver characteristics, and so forth, necessary.

(*j*). If the receiver is calibrated, the audio measurements can be evaluated in terms of radio-frequency noise-voltage input to the receiver-antenna terminal.

Such use of an audio noise meter is covered in detail under "Audio-Frequency Measurements." In the following discussion regarding radio-frequency measurements it must be noted that an audio noise meter or a pair of headphones with any aircraft-communication receiver will serve in qualitative determinations. If the receiver is calibrated in terms of audio output as a function of sine wave, 30 per cent modulated radio-frequency input, the combination will also serve for quantitative measurements when used with the receiver connected to its regular (aircraft) antenna. Such calibration will, of course, correspond only roughly to a calibration made into a noise source. The difference is, at the moment, of no great concern. The receiver used in this manner gives the truest possible analysis of the effect of radio noise on its output and also gives one more reason for testing the installation with the receiver to be used.

inertia of the load is high, this peak value I_1 will be limited only by the resistance of the armature circuit. The total time of acceleration is T_1, as shown on the graph. Curves 2 and 3 represent the average value of starting current as a function of time with the current limit set at three times the rated current I for curve 2 and two times the rated current for curve 3. T_1, T_2, and T_3 represent correspondingly the time of acceleration for each particular curve. It is apparent that, the lower the current limit, the longer will be the time of acceleration and the smoother the starting of the motor. By means of the potentiometer *P*6 (see Figure 10), the current limit can be varied and in that way the starting torque, the time, and smoothness of acceleration can be controlled.

When the armature contactor *CR* is being opened, it can be seen that the voltage across the biasing potentiometer *P*6 will drop instantly to zero, and the current-limit tube 5 will start conducting full current as soon as *CR* contacts start to move. The plate current of the master control tube 3 thus will rise to its full value corresponding to zero grid voltage, and, consequently, the rectifier tubes 1 and 2 will stop conducting, so that only a slight spark can appear on the main armature contacts, and no arcing will occur. It may be interesting to note that, owing to such a circuit arrangement, the main armature contacts need not be designed to handle the actual rupturing of the armature current, and this will often permit the use of contactors of smaller size to handle the armature circuit.

Conclusions

The control circuits described in this paper have included only basic elements needed for full understanding of the performance of the drive. The addition of a few more elements will secure a number of additional functions and features. A complete commercial control as shown in Figure 13 will usually include means for motor reversal, dynamic braking, antiplugging, and extension of speed range above the base speed by field weakening.

A control like that is a package unit complete in itself, and it incorporates all necessary devices to provide adequate protection against sustained overloads, low voltage, and short circuits.

The drive is normally controlled from a remote pushbutton station which contains forward, reverse, and stop pushbuttons and speed adjuster.

The important characteristics and features of the modern rectifier drive are as follows:

1. Wide and stepless speed range.
2. Automatic regulation to a preset speed.
3. Full torque at low speeds.
4. Smooth shockless current-limit acceleration with full control of starting torque.
5. Automatically controlled reversal of the motor.
6. Several preset forward and reverse speeds, if desired.
7. Elimination of all rotating parts except the motor.
8. Convenient remote mounting of small control stations.
9. Operation from a conventional a-c line.

References

1. Theory of Rectifier D-C Motor Drive, E. H. Vedder, K. P. Puchlowski. AIEE Transactions, volume 62, 1943, pages 863–70.

2. Thyratron Control of D-C Motors, G. W. Garman. AIEE Transactions, volume 57, 1938, June section, pages 335–42.

3. Electronic Speed Control of Motors, E. F. W. Alexanderson, M. A. Edwards, C. H. Willis. AIEE Transactions, volume 57, 1938, June section, pages 343–52.

Radio-Noise Elimination in All-Metal Aircraft

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I. Introduction

RADIO noise is the result of interaction among four components of the airplane, namely, electrical equipment, the radio receiver, the electric wiring, and the antennas. These components are produced by three different groups of manufacturers who have until recently made no effort to co-ordinate with each other on the radio-noise problem, the solution of which requires that these components be considered conjointly and that each party accept his part of the total responsibility. Until recently, this joint responsibility was not recognized, but each party was held to a rigid set of specifications which restricted them all to a very narrow line of behavior and made them responsible only for the meeting of some specific requirements. As a consequence, the various manufacturers considered their obligation as fulfilled when these specific requirements were met, whether the radio system was noise-free or not.

With the advent of wartime demands for increased production rates, there is a tremendous added incentive to adopt any feasible method which promises to expedite construction and save material. The increased use of special radio equipment requires that this problem be approached from an engineering standpoint in order that the most economical use may be made of available radio-noise-elimination methods. An intense eleventh-hour effort must be made to apply these radio-noise-elimination methods, so that obsolete practices can be replaced by modern-design practices, and the designer will accept the responsibility for them. This may be done by the application of well-known principles of circuit coupling, shielding, and filtering to aircraft electrical systems.

Shielding as a means of radio-noise reduction was first applied to ignition systems which were found to be intense sources of radio noise.[1–3] The exacting requirements of this type of shielding led designers to believe that these methods must be applied meticulously throughout the airplane. This erroneous assumption led to the specification of electrical systems which were entirely enclosed in metallic shielding, with extreme and unnecessary requirements of electrical conductivity of the shielding elements. (Junction boxes, conduit, and bonding connectors.)

It is noteworthy that the utilization of such measures has not resulted in radio-noise-free systems. The main result has been to make these burdensome requirements absolutely prohibitive under wartime conditions. Consequently, this has forced a reconsideration of these requirements, particularly regarding the installation of conduit for circuit shielding.

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(*b*). *Magnetic Fields; Use of Loop Probe.* Frequently a magnetic field will be present and the electrostatic probe will fail to give any direct indication of its presence except that this field constantly obscures electric-field measurements. In order to measure the magnetic field directly, the electromagnetic probe may consist of a half-dozen turns of wire, three inches in diameter and may be mounted either on the end of a coaxial cable or connected to the test apparatus by a twisted pair of wires.

It will be found that this probe is used in much the same manner as an ordinary loop, except that the directional effect is absent because of the multiple reflections from the airplane structure. Instead, it is necessary to determine the location of the coupling source by changing the position of the probe. The nearer it is brought to the coupling source the greater will be the pickup. It may be necessary to place a capacitor (by trial) in series with the loop in order to match the input of the receiver more closely. No attempt should be made to keep the input circuit trimmed to the loop, as very slight movements of the loop will throw the circuit off trim. Actually, it is better to use the loop without the matching capacitor, since the receiver input is so far out of trim that changes in loop impedance attributable to the proximity effect of structure have little effect.

(*c*). *Measurement of Filter Attenuation.* It is well known that the attenuation characteristics of any electric filter are dependent upon the impedances of the input and output terminations.[18] In the practical case, it is almost impossible to obtain a meaningful determination of these impedances because the noise source is a very complicated pattern of resonant and antiresonant impedances over the radio-frequency spectrum. The same is true of the impedance looking into either end of the supply line as well as when looking into the receiver at its supply terminals. Formalized filter formulas are of little use in the design of such units, hence most filter design has proceeded on a cut-and-try basis. A partly standardized method of filter measurement is that shown in Figure 4D. The (400-cycle modulated) signal generator is terminated in 30 ohms in order to maintain the accuracy of the output attenuator. The 280-ohm series resistance is used to decouple the generator from the filter. The 20-ohm shunt impedance represents the input termination of the filter. This value is, of course, highly arbitrary, since the radio-frequency impedance of the device to which the filter is connected is highly variable and is extremely difficult to determine, as such determination must be made with the instrument operating into its normal load.

It should be noted that the resistances used for the decoupling network must be noninductive up to the highest test frequency used.

The filter-attenuation data may be obtained by holding constant input to the filter and measuring the output as a function of frequency. This method is unsatisfactory, however, because the range of output amplitudes encountered requires scale multipliers to be used with the indicating meter. Scale errors with this method are considerable. The recommended method is to hold a constant signal voltage at the noise-meter input terminals and to adjust the signal generator attenuator to produce this constant voltage, at each frequency, with the filter first connected and then disconnected from the circuit. The signal generator attenuator can be depended upon for much better accuracy than the scale reading of the noise meter. With this method any radio receiver can be used as a constant-output-level indicating device. Attenuation-curve determinations made in this manner will agree within the range of 10 to 20 decibels with like measurements made when connected to the RIV source, but the agreement will hardly be better than this.

XI. Radio-Noise-Output Measurements

To meet the requirement for an instrument usable in the quantitative analysis of noise components in the audio output of radio receivers and to meet the requirements of the United States Army and Navy for an instrument for use in aircraft-radio-noise acceptance tests, an audio noise meter was developed. The circuit of this instrument is shown in Figure 5 and the frequency response characteristics of the circuit are shown in Figure 6.

This instrument is a vacuum-tube voltmeter reading a quasi-peak value of the noise signal. Only the nuisance value of the noise signal present in the audio output of a receiver is of interest, and this instrument more nearly approximates this value than does an rms reading instrument.[17] The proper response has been accomplished by the use of a resistance-capacitance network proportioned to require ten milliseconds charge time, and 600 milliseconds discharge time in accordance with the standard time constant for noise-measuring instruments.[13]

A filter limits the frequency response of the instrument to the frequency band required for the intelligible transmission of speech. (See Figure 6.) This was found necessary to prevent frequencies which contribute nothing to the nuisance value of the audio voltage under measurement from producing meter indication. The meter is calibrated in decibels using a 0-decibel reference level of one milliwatt in 600 ohms. By means of an input attenuator, the full-scale range of the meter is extended from −32 decibels to +28 decibels in steps of four decibels. The −32-decibel sensitivity is necessary in order to indicate the background level of a receiver under test, since the background level of the receiver is one factor determining the basis on which quantitative audio measurements are made. To conserve battery life and to make the instrument more portable, low current drain filament tubes are employed.

A headphone jack is provided across the input connection of the instrument so that the audio output signal of the receiver may be monitored. In use, the audio signal is monitored at the input of the audio noise meter to avoid the possibility of tuning the radio receiver to a radio station and thus obtaining false readings.

Monitoring is also of value to help identify the source of radio noise, as certain equipment items produce an audio noise signal in the output of a receiver which is characteristic of that particular equipment item.

The audio noise meter is made as small and as rugged as possible. The over-all dimensions are 12 by $6^1/_2$ by 5 inches. All batteries are self-contained and have 30 hours capacity.

1. AUDIO-NOISE OUTPUT AS A FUNCTION OF RADIO-NOISE INPUT

The use of the audio noise meter as a means of quantitative evaluation of the nuisance value of radio noise affecting a radio receiver is now to be considered.

A fundamental starting point of this evaluation is the background level of the receiver measured with all noise sources inoperative, the receiver-antenna lead-in disconnected, and the antenna terminal of the receiver grounded to the receiver chassis through a capacitor simulating the capacitance of the antenna. The background-level determination must be made at frequencies throughout the range of the receiver as the background level varies greatly with frequency.

Having determined the receiver background level with no input signal, it is then possible to determine the relative noise output produced by various RIV sources and evaluate measures taken to reduce coupling paths which contribute to receiver-noise output. Coupling sources whose effect is or has been reduced to the receiver background level need not be given further consideration.

In the case of evaluating ignition noise coupled to the antenna, the background level must be measured with the lead-in and antenna normally connected. The limiting background level in this case is the atmospheric noise level, and since the atmospheric noise level is constantly changing, such measurements cannot be taken as a fixed reference level. Thus, if the radio noise, because of ignition or other noise influences on the outside of the airplane, are reduced to the background level at one time, they may be above the background level at another time because of a reduction of the atmospheric noise level.

In order to consider the basis for the determination of permissible radio-noise-output levels, it is necessary to review the calibration curves of a typical aircraft receiver with particular consideration of ratios of signal-to-signal plus noise (hereinafter shortened to "signal-to-noise ratio").

The sensitivity of a receiver in microvolts is defined as the input voltage in microvolts required to produce a 50-milliwatt output with a signal-to-noise ratio of at least six decibels.

Calibration curves of a typical receiver are given in Figure 7. These curves are obtained by feeding a radio-frequency sine wave signal, modulated 30 per cent at 400 cycles, to the receiver from a signal generator connected to the receiver through a standard dummy antenna.

Curve *A* of Figure 7 represents the calibration curve of this receiver with the volume control 50 per cent full open in terms of knob rotation. This curve is similar to the curve obtained with the volume control full open on a receiver having insufficient audio gain to take full advantage of the sensitivity of the radio-frequency channel. This is determined by the fact that the bend of the curve is caused by the background noise of the audio channel instead of the background level of the radio-frequency channel. The background level of the receiver with no signal is seen to be about 0.02 milliwatt. If this is arbitrarily increased six decibels by noise voltages the level will come up to 0.06 milliwatt. This total noise level is very low, and the desired signal can be received with a good margin over the background noise, provided it is of sufficient intensity. A noise voltage of 9.5 microvolts is allowable, but a signal voltage of 200 microvolts will be required to make the output level 50 milliwatts, which is approximately the level required with this receiver for reception in an aircraft during flight. If this signal level is not obtainable the volume control must be advanced to obtain the required output level.

Advancing the volume control brings the operating curve to that of curve *B* of Figure 7. The background level indicated by the flattening of this curve to the left is caused by noise inherent in the first radio-frequency stage or by improperly filtered rectifier noise. This level is seen to be three milliwatts; a six decibels rise in output level brings it to ten milliwatts. A noise voltage of approximately 1.5 microvolts will accomplish this, while a signal voltage of 7.0 microvolts will suffice to raise the output level to the required 50 milliwatts. Therefore, there is still a good margin between signal and noise. In fact, the usable audio gain could even be slightly higher than that shown.

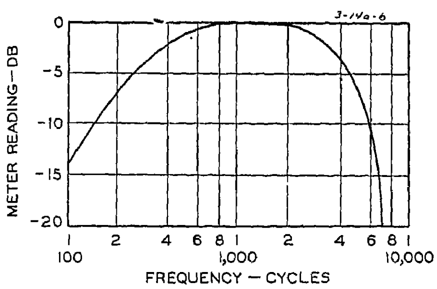

Figure 6. **Audio-noise meter frequency response**

So far, the effect of atmospheric noise level in limiting reception when the receiver is used in an airplane has not been discussed. The minimum level ranges from 2 to 10 microvolts per meter. Assume that a field strength of two microvolts per meter will place a noise voltage of four microvolts at the input terminals of the receiver, resulting in three milliwatts of noise output. Reception, even under the best conditions, is limited to a radio-frequency signal of at least two microvolts, hence we have one basis from which to determine permissible noise levels from RIV sources. Permissible noise levels for each specific receiver installation are now under investigation.

The effect of raising the audio gain above that required to obtain the full benefit of the sensitivity of the radio-frequency channel is illustrated by curve *C* of Figure 7. Here, the background level with no signal is seen to be 200 milliwatts. A six-decibel increase in background level will increase the output nearly to the receiver saturation point, so that any signal received would be barely intelligible above the noise.

From the operator's point of view, the maximum amount of audio gain, in proportion to the radio gain available, is obtained when the background level is six decibels below 50 milliwatts, or whatever level is required to give a proper margin above acoustic noise (engines, and so forth). This case is illustrated in curve *D* of Figure 7, which represents excess audio gain, indicated in curve *C*, reduced by lowering of the volume control to the precise point at which a six-decibel signal plus noise-to-noise ratio is obtained.

The radio-frequency signal is seen to be 0.6 microvolts, and the sensitivity of such a receiver is 0.6 microvolts. This is about eight decibels better than that required under the best possible atmospheric conditions. Obviously there will be nothing gained in designing a receiver with better than a one-microvolt sensitivity as long as the minimum atmospheric noise will exceed this (at the terminals of the receiver).

In making other radio-noise measurements the above considerations also apply in great measure. It is obviously undesirable to have the ratio between audio and radio gain at such a figure that a small noise voltage will send the receiver up to the audio-overload point. It is equally undesirable to have the background level so low that an inordinate signal voltage is required to drive the output stage fully.

(*a*). *Permissible Audio Noise Level Utilizing the Background Level of the Receiver as a Reference.* This method is a logical development from the previous considerations and is the simplest test that can be made. With the audio noise meter connected to the output of the receiver and switched to the proper termination, 600 or 4,000 ohms, turn the volume control full on and read the background level with the audio noise meter. Then operate a test noise-influence source whose level is great enough to overload the receiver. There should be at least an output level increase of 20 decibels to prove that operation is not confined to the limited volume range indicated by curve *C* of Figure 7.

If this is not obtained with the receiver at maximum sensitivity, either the noise-voltage input is too small or the receiver is operating too close to the overload point, as indicated by curve *C*.

It is preferable, at least for small noise voltages, to operate the receiver on the linear portion of the curve. Also, when noise tests are being made, if the noise voltages are high and the receiver is operated close to the overload point, as in curve *C*, it will be impossible to determine whether corrective measures have reduced the noise voltage input from 100 to 10 microvolts. In such cases, the gain control must be retarded until the noise voltage does not bring the receiver to the overload point.

The test may now proceed with respect to the noise sources in the airplane. It is suggested that noise-reduction measures be employed until the remaining noise level is within three decibels of the background level of the receiver alone. The disadvantage of this method is that noise reduction may be carried to much lower noise levels than actually required in practice. The following method eliminates this difficulty.

(*b*). *Permissible Noise Level on the Basis of Power Level Required for Reception.* This method requires that a family of curves such as those given in Figure 7 be available for each frequency tested. Also, an estimate of the output power level required during operation of the airplane will be necessary. For example, some requirements are that for operation in a cockpit with 120 decibels of noise, there should be available at least 35 to 50 milliwatts of signal output. With these data as a reference base, the receiver is placed in operation and the output level read. Considering the conditions as represented by curve *B*, the output level with no signal input is seen to be about four decibels (reference 0 db = 1 mw in 600 ohms). Until permissible noise levels are established, it is just as well to assume a value of six decibels as a proper margin.

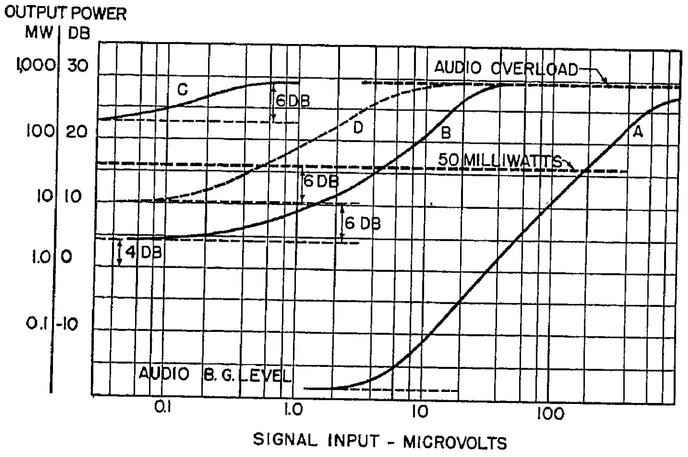

Figure 7. Typical aircraft-receiver calibration curves

Referring to curve *B*, six decibels below 50 milliwatts is seen to be about 12 milliwatts which corresponds to input noise voltage of about 1.5 microvolts. Utilizing this method it is possible to make measurements with a radio noise meter directly connected to the antenna, provided the antenna can be terminated in an impedance representing a receiver. This will then give a correlation between radio-noise-voltage input and audio noise output of the receiver.

This method still does not take atmospheric noise levels into consideration. As more work is done on this subject, the present arbitrary assumptions with respect to permissible noise levels will be replaced by actual values determined by test.

(*c*). *Determination of Paths of RIV Coupling to the Radio Receiver.* There are three paths of coupling to a radio receiver through which radio-noise influences may produce receiver-noise output; namely, the antenna lead-in, common impedances internal to the radio receiver, and common receiver-bonding impedances. Application of any noise-elimination method should be made only after each of the coupling paths has been evaluated separately.

RIV coupling through the medium of the common impedance formed by the receiver bonding connector(s) may be evaluated by disconnecting the antenna lead-in from the receiver-antenna terminal and connecting the antenna terminal to the receiver chassis. This connection eliminates coupling to the antenna lead-in and coupling because of the common impedance formed by the receiver-bonding connection. A reference reading is taken on the audio noise meter with the RIV source operating, and the antenna terminal is then connected to the aircraft structure instead of the receiver chassis. Any increase in receiver output (antenna terminal connected to structure) above the reference level (antenna terminal connected to receiver chassis) is due to RIV drop across the common impedance formed by the receiver-bonding connector(s). This form of coupling may be controlled by bonding the receiver to structure by means of the shortest possible bonding connectors. It should be noted that the radio-frequency impedance of a given bonding connector is proportional to its length and for a given length the impedance increases as the square of the frequency. Hence, every effort should be made to reduce the length of receiver bonding connectors to an absolute minimum.

Having controlled one coupling path, RIV coupling through the medium of common impedances internal to the receiver may now be evaluated by connecting the receiver-antenna terminal to the receiver chassis. Any increase of receiver output when RIV sources are operated is due to RIV drop across common impedances internal to the receiver. If this type of coupling is found to exist, it may be controlled by the application of RIV filters in the receiver power or remote-control wires which are responsible for carrying the radio-noise-influence currents into the receiver.

The antenna lead-in may now be reconnected to the receiver and evaluation made of RIV coupling to this conductor. This is done by noting the audio-noise-meter reading with the RIV sources operating and not operating. Any increase in receiver output level when the RIV sources are operating will be due to RIV coupling to the antenna lead-in. Coupling to this conductor may be controlled by separating physically the lead-in from wiring carrying radio-noise-influence currents, by shielding wiring carrying radio-noise-influence currents in the immediate vicinity of the lead-in or by shielding the antenna lead-in conductor.

Evaluation of the various RIV coupling paths should be made in the order given in order to prevent masking of one coupling path by another. If tests for one coupling path are to be made without benefit of control over all other paths, the evaluation of the result must consider the effect of the uncontrolled coupling paths.

COMMENTS

This writing has not proposed to offer any basic facts not heretofore available but to assemble and apply them in an engineering manner to obtain an economical solution to the problem.

The full utilization of the methods proposed in this writing requires teaching the subject to the electrical and radio-design engineers and enforcement through written design material. This can be accomplished by writing a basic "Radio and Electrical Layout Design Requirements Manual" for aircraft electrical engineers. The purpose of this manual is to make the designer aware of the problem so that he will then seek further assistance from the proper consultants. If properly done, this manual will not conflict with Army-Navy specifications.

Such manual material should hence be of general nature supplemented by material applying to each separate airplane model. The general Army-Navy specification covering shielding and bonding is used as a basis from which to derive an engineering shielding and bonding specification giving detail practice applying only to one model as a guide to the engineer. Such an engineering specification usually incorporates deviations from the Army-Navy specification that must be specifically approved by the Armed Services.

This intermediate step is necessary to ensure that general requirements necessarily written into a basic specification are not applied to specific airplanes to which they do not apply.

The question of how to handle radio-noise-suppression-design information on drawings is one that must be settled at the individual plant. It is recommended that the central point of responsibility for radio-noise-suppression design be placed upon the trained personnel in the electrical design group. Such items as bonding jumpers must be determined specifically as to quality and placement by the trained personnel and so shown on drawings. These items must not be left to the discretion of personnel untrained in the technical aspects of this problem.

The same applies to any and all references on drawings calling for cleaning or surface treatment utilized for the purpose of obtaining incidental bonding between parts.

Conclusions

1. Proper forethought to the layout of radio equipment will pay large dividends in the economical solution of radio-noise problems.

2. In all-metal aircraft the shielding effect of the structure greatly simplifies the economical solution of radio-noise problems in comparison to those of the nonmetal aircraft and in comparison to ground-receiving stations. Shielding and filtering is reduced greatly in that its application is restricted to those few points at which disturbance occurs. In the latter cases radio-noise influences are much more apt to require treatment at their sources which are usually very numerous.

3. It is desirable to design all equipment to produce the least possible RIV, but it is meaningless to ascribe arbitrary limits to RIV as measured at equipment terminals or throughout the electrical system in an attempt to protect radio-receiver performance.

This derives because RIV measurements on installed electrical wiring or equipment do not yield a measure of radio noise output unless a direct correlation has also been determined. At the higher frequencies noise coupling is functional with current rather than voltage, and steps taken to reduce RIV may increase RIC and result in a greater coupling. Radio-frequency current cannot be measured with available instruments; hence, each specific case of coupling must be eliminated by cut-and-try methods.

(*a*). The services of radio test engineers competent to handle radio-noise problems are an absolute necessity wherever radio equipment is installed, as the individual units are not yet advanced sufficiently in design to work together with other electrical-equipment items without interference.

Once a working "mockup" has been proved, the subsequent installation then can be made at modification centers without skilled technicians. Such tailoring cannot be obviated or even helped by setting general design limits to RIV as noted heretofore and only serves to distract attention from the practical job of making specific equipment work in a specific airplane.

4. Output measurements made with the proper type audio-noise meter are the most accurate with respect to actual evaluation of the effects of radio-influence sources.

5. The requirements of simplicity for production aircraft-radio-noise measurements dictate the use of an audio noise meter and prohibit the use of a radio-frequency noise meter. The radio-noise meter is not suitable for such use but remains a valuable laboratory tool.

6. The importance of proper bonding of the receiver to structure cannot be overstressed. The achievement of receiver bonding of low radio-frequency impedance reduces filter requirements to a minimum.

References

1. Suppressing Ignition Interference on Radio-Equipped Aircraft, **E. A. Robertson, L. M. Hull.** Society of Automotive Engineers *Journal*, volume 27, July 1930, page 78.

2. Approved Ignition Shielding for Radio Installed on Commercial Planes. *Automobile Industries*, volume 63, September 20, 1930, page 408. Reprint of material in *Air Commerce Bulletin*, volume 2, number 5, September 2, 1930.

3. Radio Reception Versus Ignition Interference, **H. Hoover, Jr.** *Aero Digest*, volume 16, number 3, March 1930, page 60.

4. The Shielding Effect of National Advisory Committee for Aeronautics Cowlings, **G. H. Drake, A. W. Parkes, Jr.** *Aviation*, volume 28, number 2, January 11, 1930, page 56.

5. Electromagnetic Shielding, **Lyons.** Institute of Radio Engineers *Proceedings*, volume 21, number 4, April 1933, page 574.

6. Airplane Motor Radio Shielding, **L. C. Wolcott.** *Aviation Engineering*, June 1931, page 33.

7. Radio Shielding for Pratt and Whitney Engines. *Aviation Engineering*, March 1931, page 35.

8. Field Strength of Motor Car Ignition Between 40 and 450 Megacycles, **R. W. George.** Radio Corporation of America Communications, Inc., engineering report *F*-53-*P*-18, March 1940.

9. Precipitation Static Interference on Aircraft and Ground Stations, **H. M. Hucke,** Institute of Radio Engineers *Proceedings*, volume 27, May 1939, page 311.

10. Precipitation—Static Radio—Interference Phenomena Originating on Aircraft, **E. C. Starr.** Bulletin 10, Engineering Experiment Station, Oregon State College, June 1939.

11. Minimum Noise Levels Obtained on Short-Wave Radio Receiving Systems, **Karl G. Jansky.** Institute of Radio Engineers *Proceedings*, volume 25, number 12, December 1937, page 1517.

12. Cosmic Static, **Grote Reber.** Institute of Radio Engineers *Proceedings*, February 1940.

13. Methods of Measuring Radio Noise. Report of the Joint Committee on Radio Reception of Edison Electric Institute, National Electrical Manufacturers Association, and Radio Manufacturers Association; EEI publication *C*9, December 1935, New York, N. Y.; *G*9, February 1940.

14. Your Product Need Not Have Radio Interference, **C. W. Frick.** *Electrical Manufacturing*, volume 23, March 1939, pages 31–4; 60.

15. Instruments and Methods of Measuring Radio Noise, **C. V. Aggers, D. E. Foster, C. S. Young.** AIEE Transactions, volume 59, 1940, March section, pages 178–92.

16. Radio-Influence Characteristics of Electrical Apparatus, **P. L. Bellaschi, C. V. Aggers.** AIEE Transactions, volume 57, 1938, November section, pages 626–33.

17. Progress in the Development of Instruments for Measuring Radio Noise, **C. M. Burrill.** Institute of Radio Engineers *Proceedings*, volume 29, number 8, August 1941, page 44.

18. Aircraft Radio Noise Filters, **C. W. Frick.** *Aircraft Communications*, July 1943, pages 32–42; 75.

Turn Ratio of the Capacitor Motor

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MEMBER AIEE

IN a previous paper[1] the basic equations for the stator- and rotor-winding currents of the capacitor motor were established in terms of certain coefficients which were functions of the fundamental constants of the machine. Application of these relations to establish the current loci was made to typical design data taken from earlier papers by Morrill,[2] and Puchstein and Lloyd,[3] based on a single value of the ratio of auxiliary-winding turns to main-winding turns. It is the purpose of this paper to extend the investigation to determine the effects upon current loci, and upon certain performance characteristics, of changing the turn ratio.

Application of the basic equations is made for three values of turn ratio, and it is definitely assumed that all other fundamental constants remain constant when the ratio is changed. This implies that the ratio of copper in the auxiliary and main windings changes, and that the dimensions of the machine and the distribution of windings also change. The converse case of a constant copper ratio is reserved for future study. For the present study application is made to the four-pole 110-volt one-fourth-horsepower single-phase motor discussed in the previous paper,[1] and the machine constants are as listed there, except that k is assigned values of $^1/_2$, 1, and 2.

It was necessary to compute coefficients M, N, P, Q, U, and W for each value of k by substitution in equations 21–26 of the previous paper.[1] These computations were made for ten values of per-unit speed ranging from $S=1.0$ to $S=0.0$, and for six values of capacity reactance from $X_c=20$ ohms to $X_c=500$ ohms at each speed. The variation of the coefficients in relation to k is shown in Figures 1 to 6, inclusive, for a constant speed $S=0.96$. Values of I_a and I_b were computed for each value of speed and capacity reactance using equa-

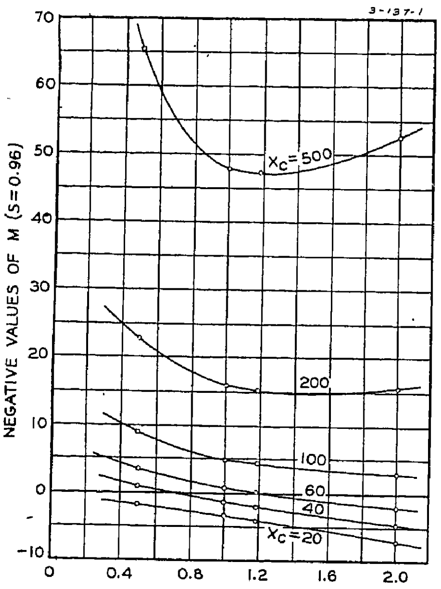

Figure 1. *M* as a function of turn ratio

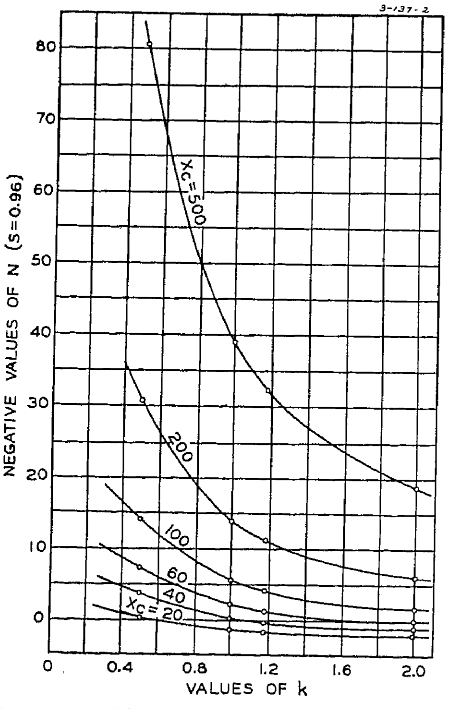

Figure 2. *N* as a function of turn rati

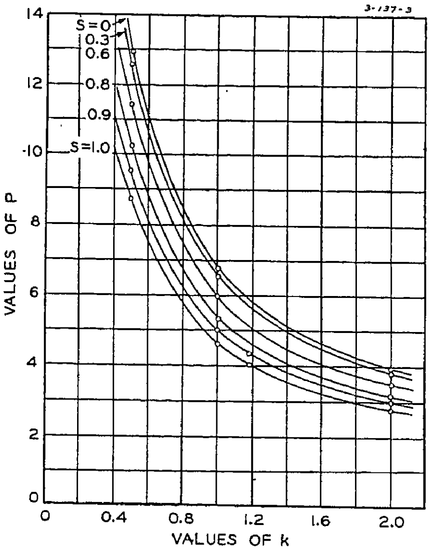

Figure 3. *P* as a function of turn ratio

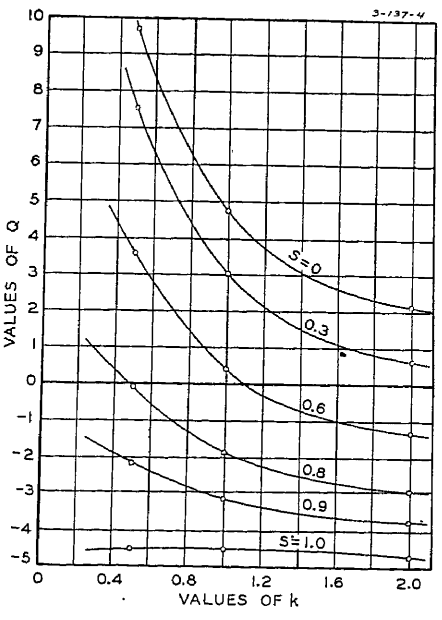

Figure 4. *Q* as a function of turn ratio

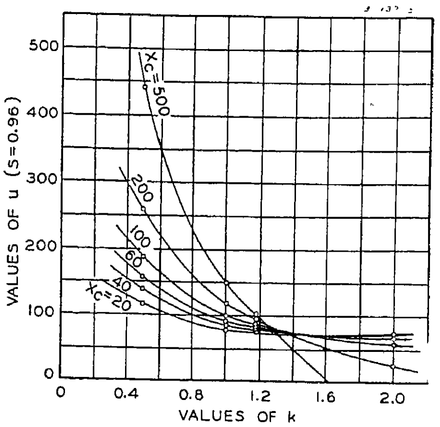

Figure 5. *U* as a function of turn ratio

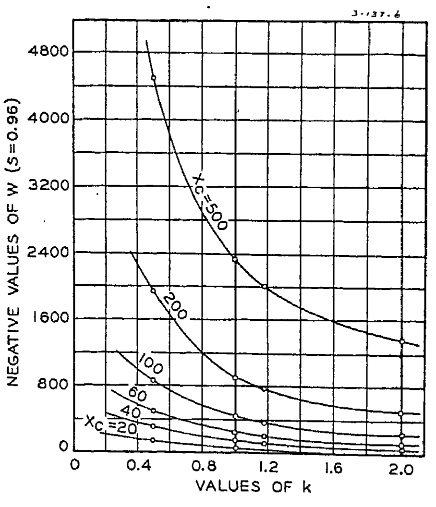

Figure 6. *W* as a function f turn ratio

Paper 43-137, recommended by the AIEE committee on electrical machinery for presentation at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943. Manuscript submitted January 23, 1942; made available for printing July 8, 1943.

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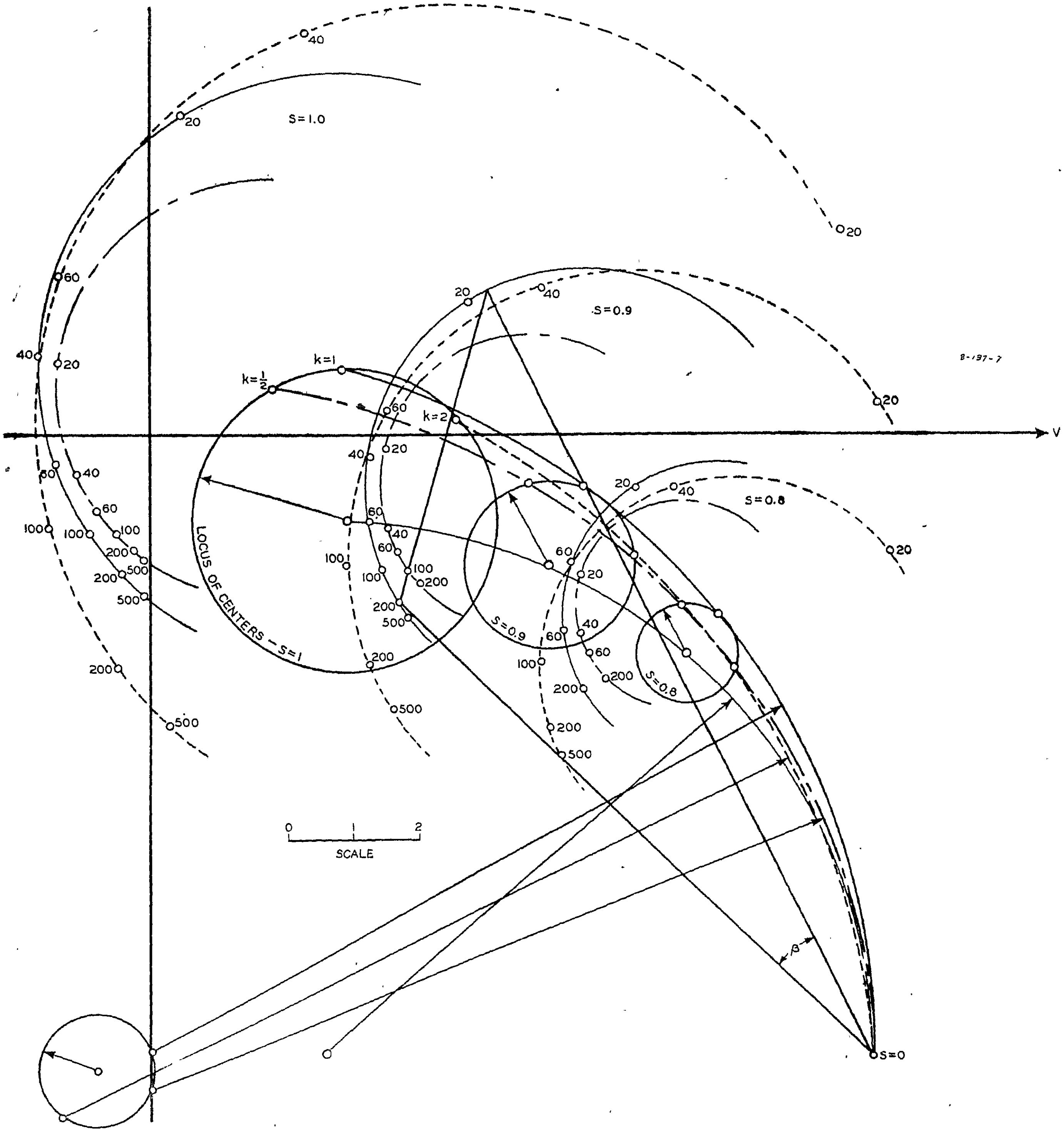

Figure 7. Current loci for main winding

tion 20 of the previous paper.[1] The values for $S=1$, $S=0.9$, $S=0.8$, and $S=0$ are plotted in Figures 7 and 8 for each value of k. Study of these figures shows that certain observations made in relation to Figures 8 and 9 of the previous paper[1] must be generalized to account for changes in turn ratio. It will be noted that

(*a*). The blocked-rotor value of I_a is independent of k.

(*b*). Current loci are circles for all values of k.

(*c*). For each fixed value of k the centers of current loci for constant S lie on a circular arc.

(*d*). The centers of circles for constant S traverse a circular locus as k is varied, and the centers of these latter loci also lie on a circular arc.

(*e*). The centers of the circular arcs passing through the centers of loci for constant S also traverse a circle as k is varied. For values of k between one half and one these centers can be assumed to lie on the axis of co-ordinates perpendicular to V (as shown in Figure 7 for I_a) and can be determined by the perpendicular bisector of the line joining the center of the circle for $S=1$ to the blocked-rotor point.

(*f*). The radii for circles of constant S are functions of k.

[1] In Figure 7 it is found that for $k=1$ the radius of any circle of constant S is $S^{2.9}$ times the radius of the circle for $S=1$. The exponent decreases from 2.9 for values of k below one and increases for

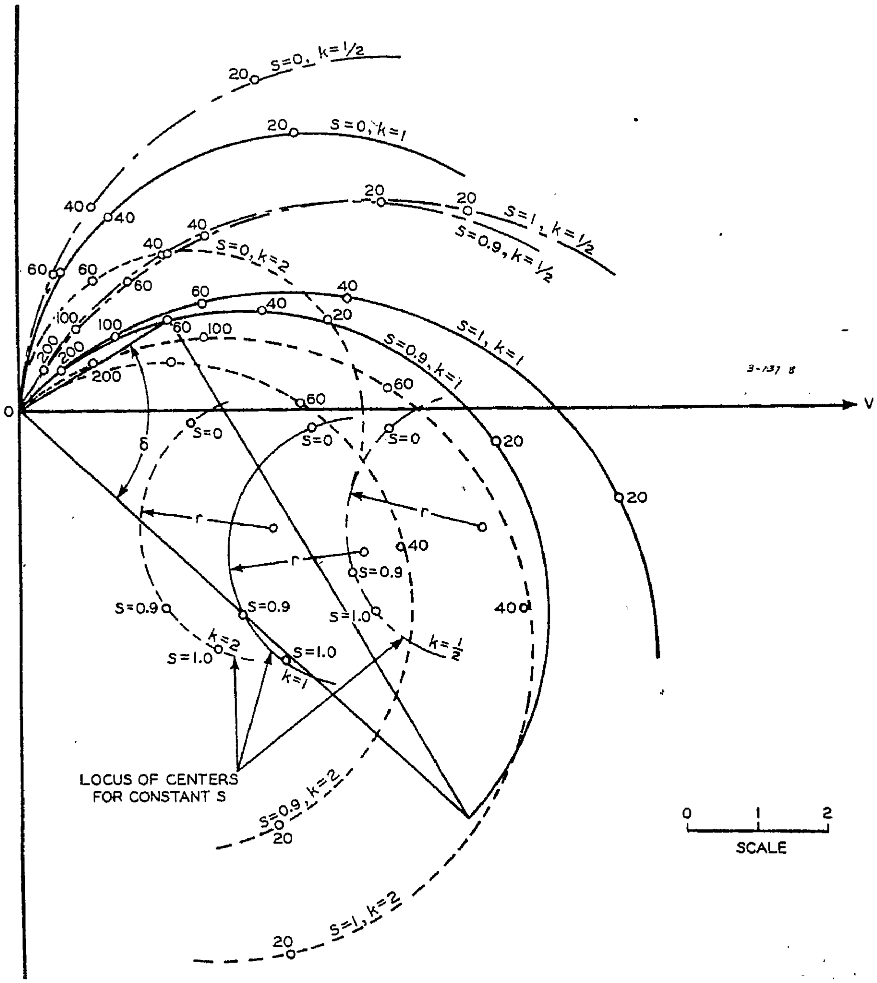

Figure 8. Current loci for auxiliary winding

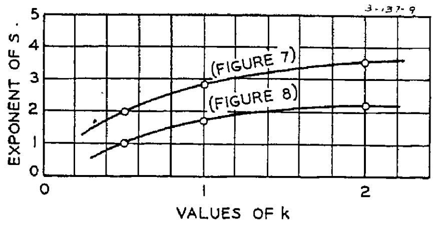

Figure 9. Exponent of S as function of k

(*g*). For constant k the angle β, determined as outlined for Figure 8 of reference 1, is constant for all values of S and values of k equal to or less than one. For values of k above one, β decreases with S. At $k=2$, β decreases in direct proportion to S.

(*h*). For all constant values of k the angle δ, determined as outlined for Figure 9 of reference 1, is constant for all values of S over the operating range ($S=0.9$ to $S=1.0$).

For each value of I_a and of I_b the power input was computed as the product of the voltage and the component of current in phase with it. The respective quadrature components of current and vars were also determined. Using equation 5 of reference 1, values of I_{2a} and I_{2b} were found. Stator copper losses for the main and auxiliary windings were computed as $I_a^2r_a$ and $I_b^2(r_b+r_c)$, respectively. Rotor copper losses for the main and auxiliary axes were found as $I_{2a}^2r_2$ and $k^2I_{2b}^2r_2$, respectively. Friction and windage losses were taken as 13 watts for $S=0.96$ and were assumed to vary as S^2. Iron losses do not enter into the computations of output since they were considered as external to the motor. Values of net output were obtained by subtracting the summation of windage, friction, and copper losses from the values of input. From these data watts output were plotted as functions of watts input for constant values of X_c, as shown in Figures 10, 11, and 12, corresponding to values of k equal to $^1/_2$, 1, and 2, respectively. Then, by cross-plotting for constant values of watts output, the curves of Figure 13 were obtained showing watts input as a function of microfarads capacitance. The line frequency was assumed to be 60 cycles when determining the capacitance from the values of X_c.

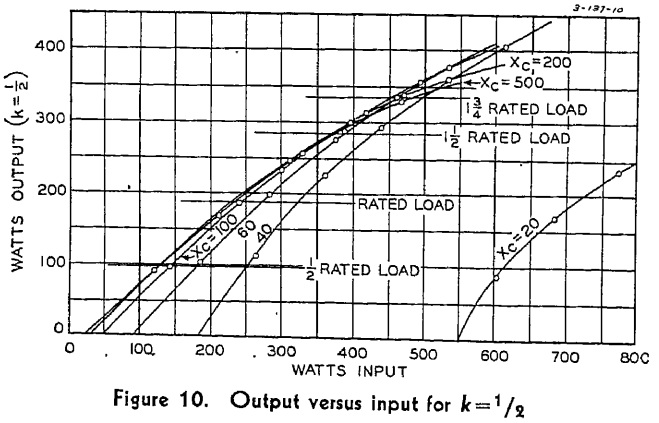

Figure 10. Output versus input for $k={}^1/_2$

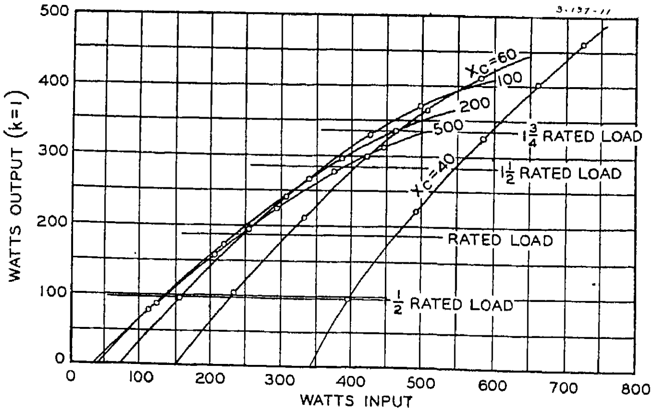

Figure 11. Output versus input for $k=1$

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values of k above one. The relation of the exponent to the turn ratio is shown in Figure 9.

2. In Figure 8 it is found that for $k=1$ the radius of any circle of constant S is $S^{1.75}$ times the radius of the circle for $S=1$. The exponent decreases from 1.75 for values of k below one and increases for values of k above one. The relation of the exponent to the turn ratio is shown in Figure 9.

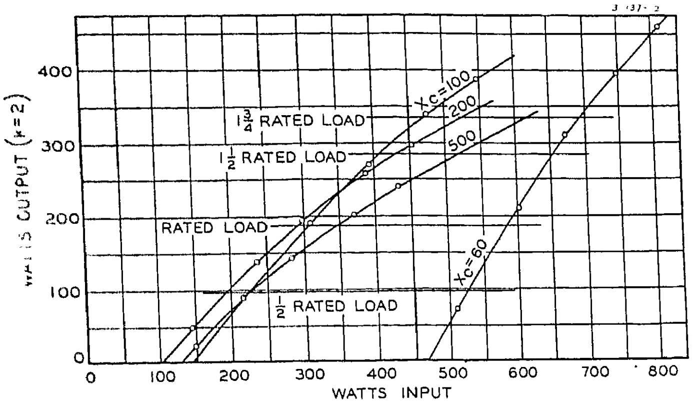

Figure 12. Output versus input for $k=2$

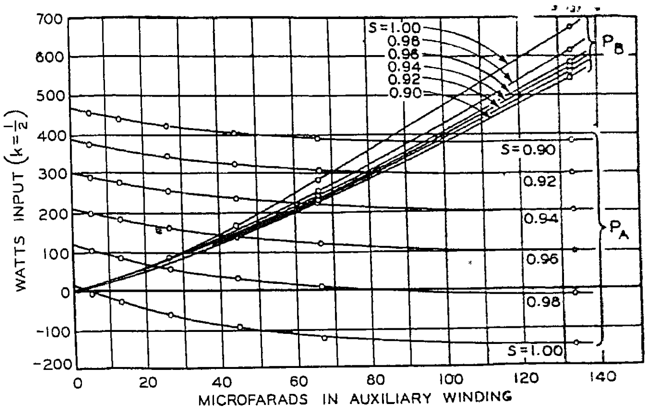

Figure 14. Input watts versus capacitance for constant speeds and $k=\frac{1}{2}$

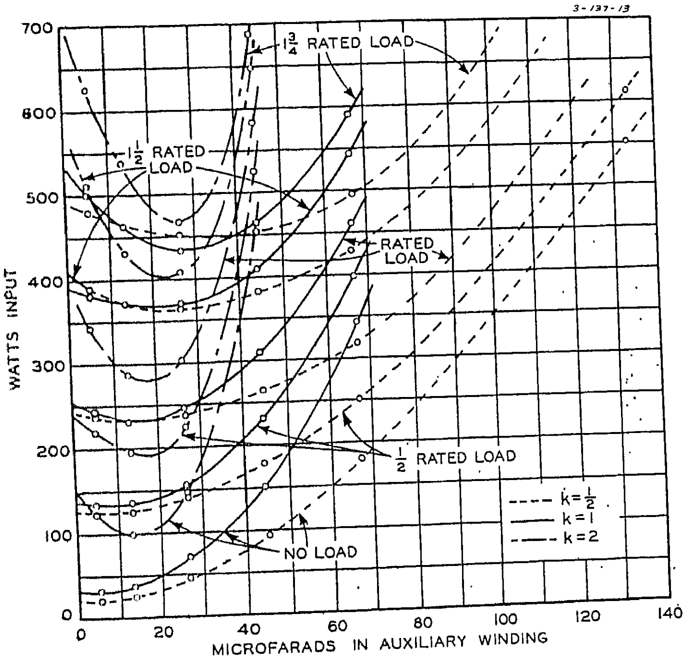

Figure 13. Input versus auxiliary-coil capacitance for constant output

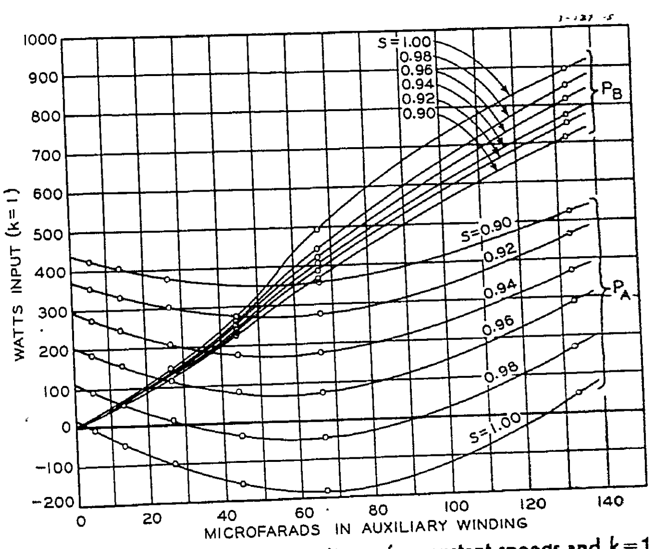

Figure 15. Input watts versus capacitance for constant speeds and $k=1$

In order to determine the values of capacitance at which the watts input to the main and auxiliary windings were equal the curves of Figures 14, 15, and 16 were plotted showing the input to each winding as a function of capacitance, for constant values of S. Intersections of the curves of like speed gave the desired values of capacitance. These values of capacitance were then plotted as functions of speed as shown in Figure 17.

By a similar procedure curves of equal (but opposite) vars for the two windings were computed and plotted as shown in Figures 18, 19, and 20. From the intersections of curves of corresponding speed the values of capacitance to produce a line power factor of unity were found. These values are plotted as a function of per-unit speed in Figure 21.

In a similar manner curves of volt-amperes for each winding were plotted as functions of capacitance. These are

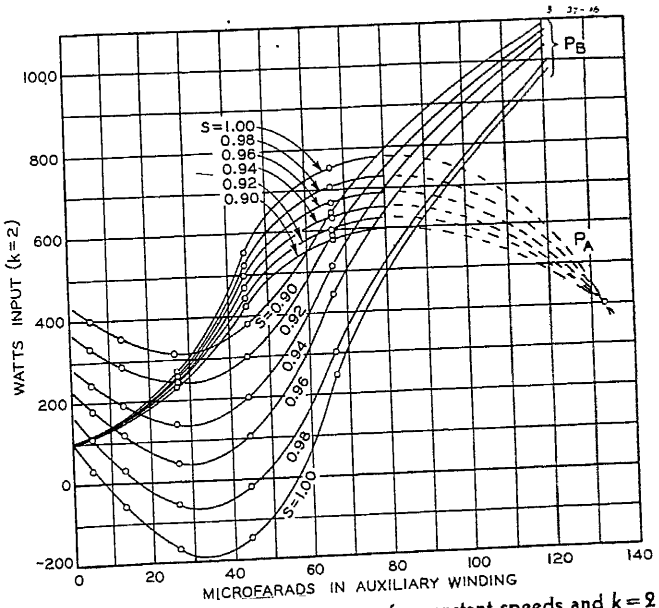

Figure 16. Input watts versus capacitance for constant speeds and $k=2$

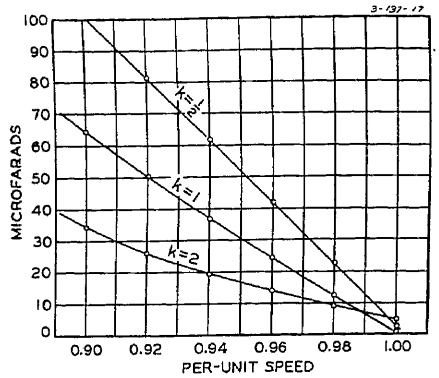

Figure 17. Capacitance versus per-unit speed for equal watts

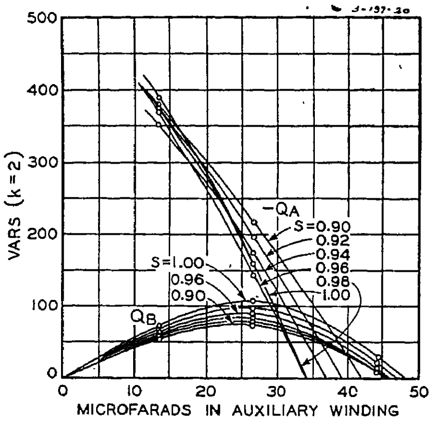

Figure 20. Vars versus capacitance for constant speeds and $k=2$

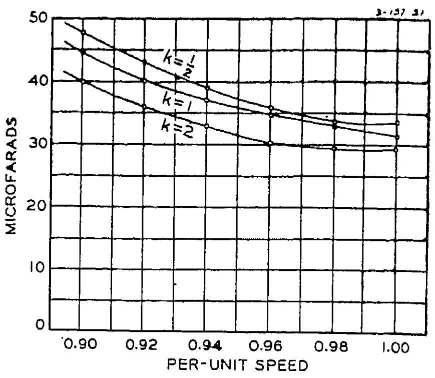

Figure 21. Capacitance versus per-unit speed for equal vars

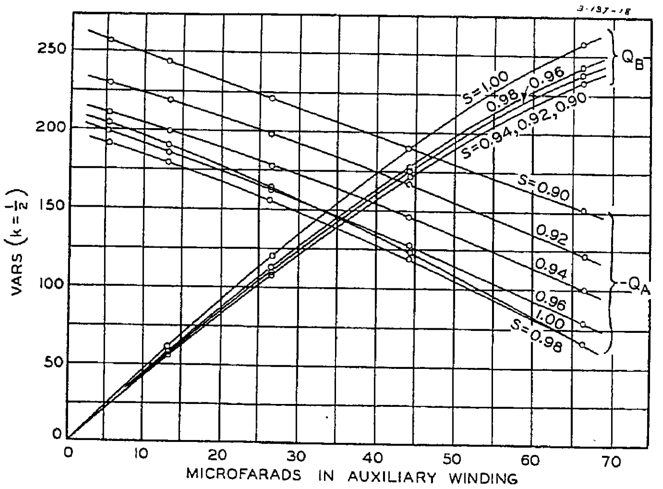

Figure 18. Vars versus capacitance for constant speeds and $k={}^1/_2$

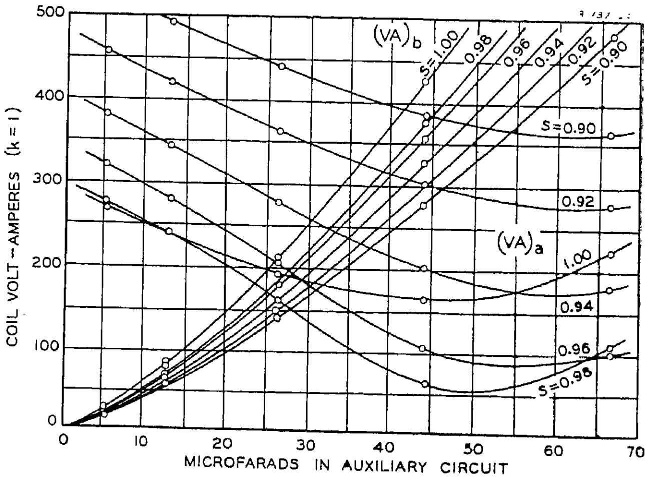

Figure 22. Volt-amperes versus capacitance for constant speeds and $k=1$

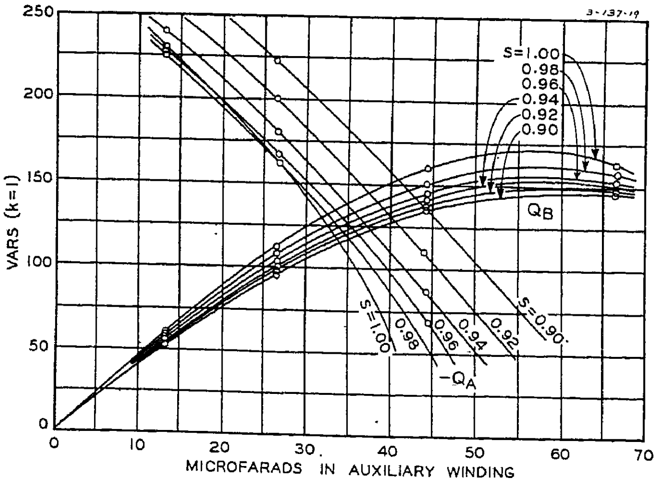

Figure 19. Vars versus capacitance for constant speeds and $k=1$

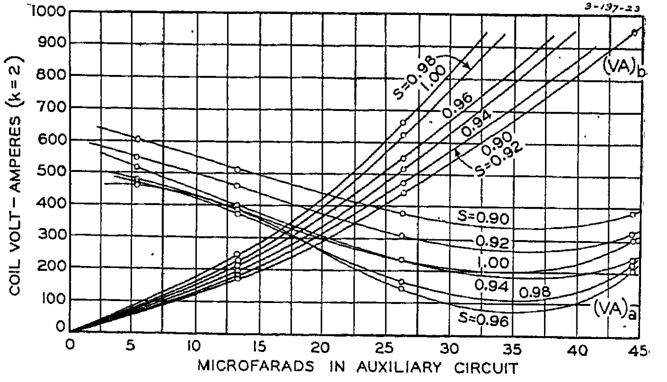

Figure 23. Volt-amperes versus capacitanc for constant speeds and $k=2$

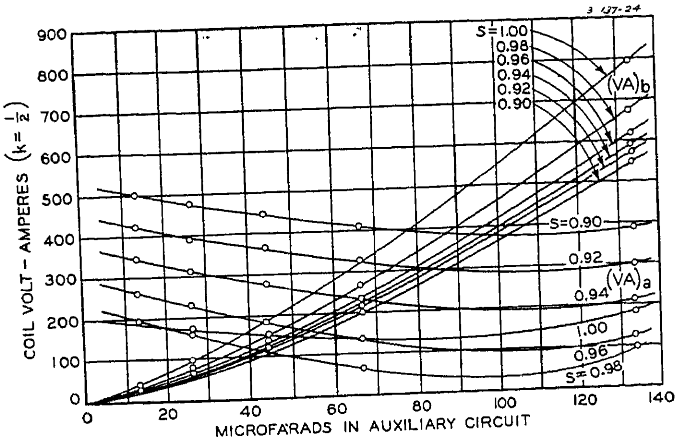

Figure 24. Volt-amperes versus capacitance for constant speeds and $k=1/2$

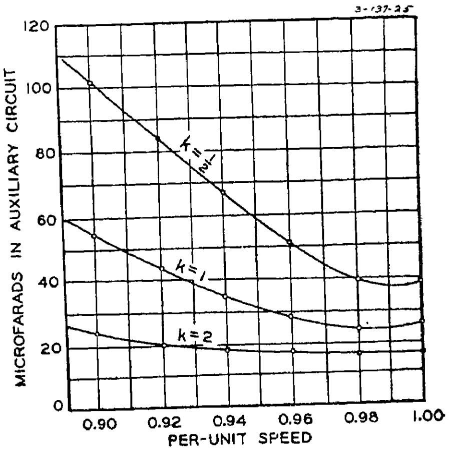

Figure 25. Capacitance versus per-unit speed for equal volt-amperes

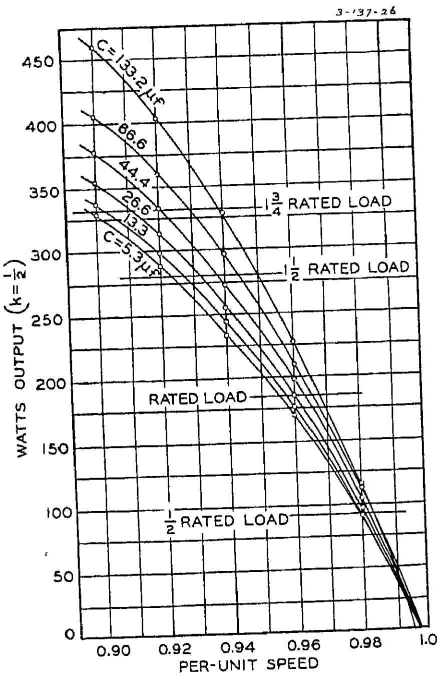

Figure 26. Watts output versus per-unit speed for $k=1/2$

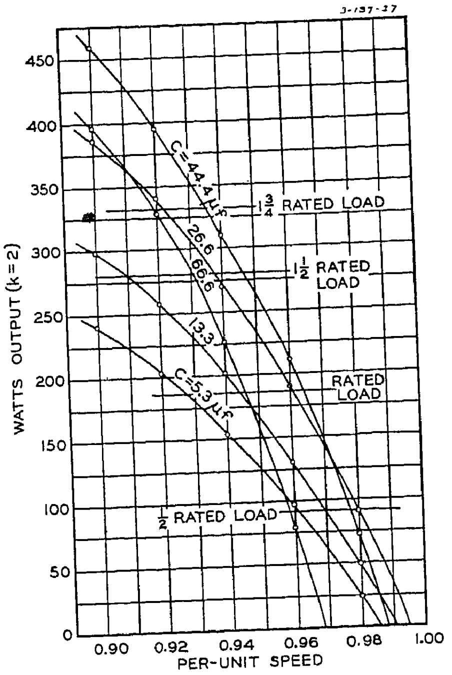

Figure 27. Watts output versus per-unit speed for $k=2$

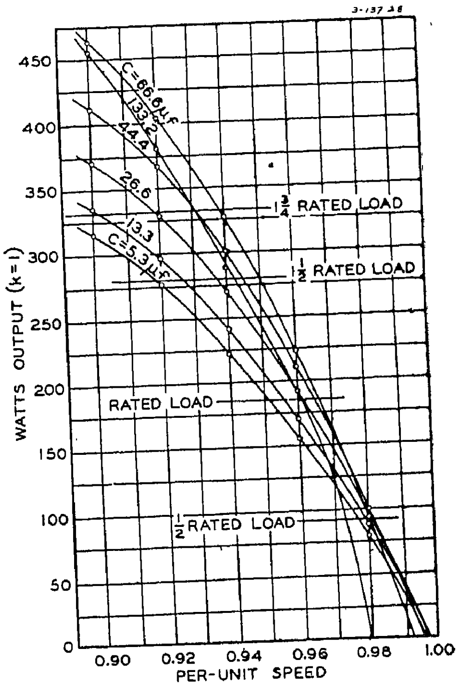

Figure 28. Watts output versus per-unit speed for $k=1$

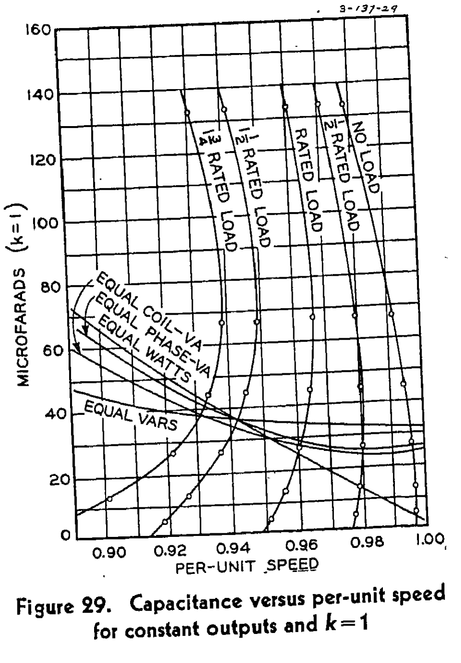

Figure 29. Capacitance versus per-unit speed for constant outputs and $k=1$

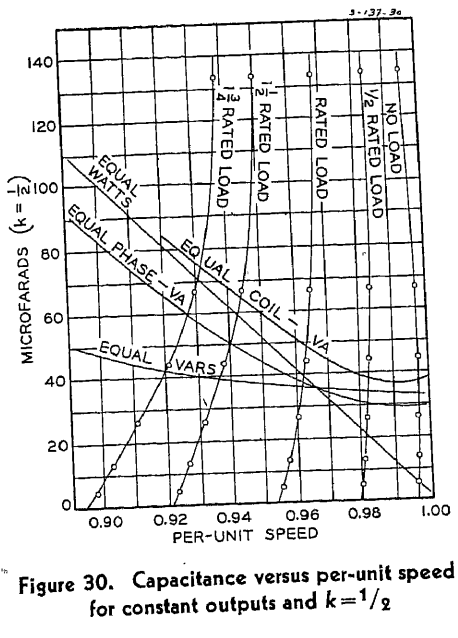

Figure 30. Capacitance versus per-unit speed for constant outputs and $k=1/2$

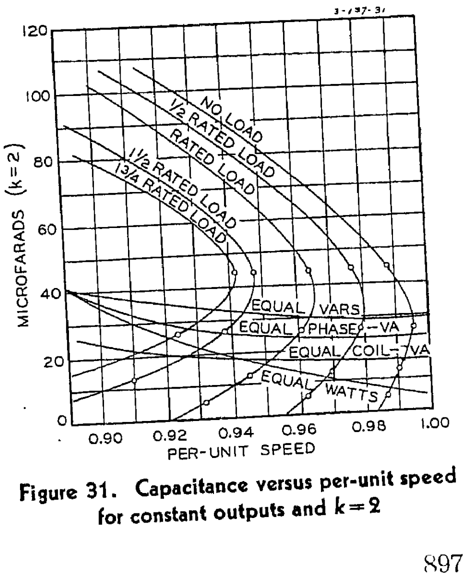

Figure 31. Capacitance versus per-unit speed for constant outputs and $k=2$

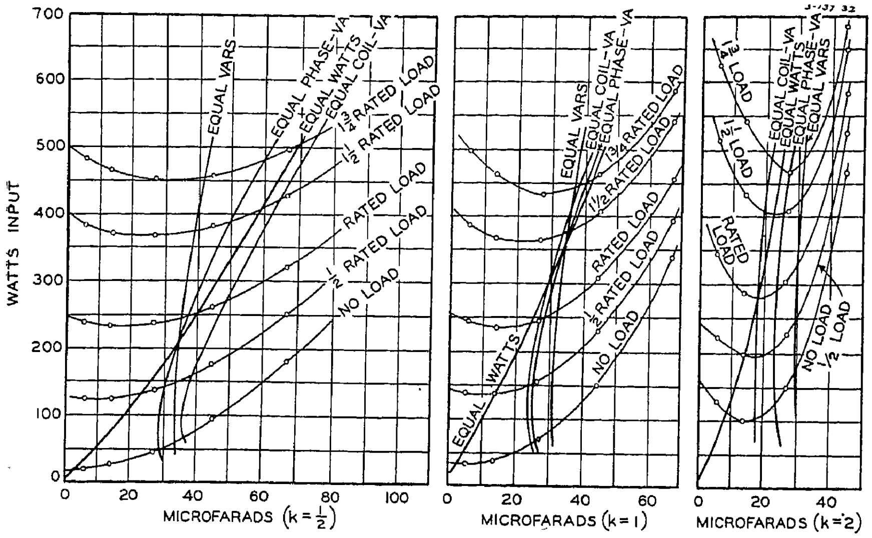

Figure 32. Equal-relations curves

shown as Figures 22, 23, and 24. From the intersections of curves of like speed the curves of Figure 25 were found, giving the capacitances required to give equal volt-amperes in the two windings at speeds over the operating range.

From the computed data of watts output the curves of Figures 26, 27, and 28 were plotted to show the net output as a function of per-unit speed, for constant values of capacitance. Cross-plotting from these curves for constant values of watts output, the curves of Figures 29, 30, and 31 were obtained, showing the microfarads as a function of speed in order to keep the output constant. It was then possible to superimpose the curves of Figures 17, 21, and 25 in order to determine intersections which would give the capacitances at a fixed output for which there would be equal watts, equal volt-amperes, and equal (but opposite) vars.

In Figure 32 the curves of Figure 13 were replotted to separate the curves according to values of *k*. Using the intersections obtained in Figures 29, 30, and 31, it was then possible to plot the curves of equal watts, equal volt-amperes, and equal vars as shown in Figure 32. These curves indicate that for turn ratios between one-half and two a design for equal watts is preferable to a design for equal volt-amperes since for all loads up to rated value the efficiencies are higher and the required capacitance is less. The line power factors are less, however, as indicated by the positions of the equal-watt and equal-volt-ampere curves relative to the equal (but opposite) vars or unity-power-factor curve. It will be noted that the equal-relations curves shift to the left as the turn ratio is increased, hence for some values of *k* above two the equal-volt-ampere design will give higher efficiencies than the equal-watts design. Since for these high turn ratios the variation in capacitance between the equal-relations curves is not great, it is probable that when first cost, efficiency, and line power factor are all considered the equal-volt-ampere design is to be preferred.

References

1. CURRENT LOCI FOR THE CAPACITOR MOTOR, **Thomas C. McFarland.** AIEE TRANSACTIONS, volume 61, 1942, March section, pages 152–6.

2. THE REVOLVING-FIELD THEORY OF THE CAPACITOR MOTOR, **Wayne J. Morrill.** AIEE TRANSACTIONS, volume 48, 1929, pages 614–29.

3. THE CROSS-FIELD THEORY OF THE CAPACITOR MOTOR, **A. F. Puchstein, T. C. Lloyd.** AIEE TRANSACTIONS, volume 60, February section, 1941, pages 58–62.

4. DESIGN OF CAPACITOR MOTORS FOR BALANCED OPERATION, **P. H. Trickey.** AIEE TRANSACTIONS, volume 51, 1932, pages 780–5.

5. THE EQUAL-VOLT-AMPERE METHOD OF DESIGNING CAPACITOR MOTORS, **P. H. Trickey.** AIEE TRANSACTIONS, volume 60, November section, pages 990–2.

Aircraft Transformers—Small and Light

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THE purpose of this paper at the present time is to make available for the aircraft industry knowledge and information on the newest types of transformers specially developed to obtain extreme small size and the lightest possible weight. It is felt that the aircraft engineers should be given every opportunity to take advantage of the latest developments without the delay of extensive research. Every effort has been made to comply with their persistent demands for equipment as small and as light as possible.

Introduction

The necessity for light weight and small size in aircraft transformers is very evident, since any reduction in space and weight will reflect in larger capacities for fuel and bomb loads. Various types of transformers have been developed recently, such as, three-phase to single-phase, single-phase to three-phase, two-phase to three-phase, three-phase to two-phase, combination a-c to d-c transformer-rectifier self-regulated units, miniature instrument type, and a line of general purpose aircraft units with kilovolt-ampere ratings ranging from 100 volt-amperes to five kilovolt-amperes, with weights ranging from 0.8 pound to 9.5 pounds and operating at ultimate temperatures of 225 degrees centigrade. Included are vibrator inverter transformers, voltage stabilizers, high-voltage and low-voltage booster ignition coils.

General Features

The construction used on many of these designs features open core and coils and screw-type terminals. On those units intended for high temperature operation the terminal boards are made of Mycalex, and all internal connections are joined with silver solder. For normal temperature design, cloth-base Textolite is employed in terminal-board construction.

In design, careful attention is given to locate the center of gravity as low as possible and as near the mounting surface as practicable. All units are also designed to withstand vibration tests specified by the United States Army and Navy.

Core materials have been selected to operate at high flux density but not to saturate. Laminations ten mils thick, which provide for reasonable losses and allow practical manufacturing, are chosen for 400-cycle designs.

The choice of insulating materials is most important to permit operation of aircraft units at high temperatures far above those in past practices, furthering the primary object of weight reduction. All insulation in the special high-temperature units, including winding spools, layer insulation, wrappers, and conductor-turn insulation, is glass cloth and glass fiber impregnated with a specially developed resin capable of operating at 225 degrees centigrade continuously.

To prevent oxidation of the copper wire at elevated temperatures the wire is cadmium plated prior to application of the glass fibers.

Overloads for short periods are permitted, during which the temperature may exceed operating values. This means that $1^1/_2$ to 2 times normal loads can be tolerated for a few minutes with no damage to the transformers.

Life tests have been completed on a four-pound two-kilovolt-ampere unit which operated in excess of 1,000 hours at 225 degrees centigrade. The unit was exposed to 95 per cent relative humidity, while cool and not operating, for eight hours in each 24-hour cycle. At the end of the life test the efficiency and heating had not changed appreciably, and continued operation was satisfactory.

Table I. Heating Data for Various Altitudes With and Without Dural Plate

Altitude	Load in Watts	With Dural Mounting Plate	Ambient Temperature (Deg. C.)	Ultimate Temperature (Deg C) Outer Coil	Inner Coil	Top of Core
Sea level	2,000	Yes	30	172	208	172
Sea level	2,000	No	30	188	245	212
12,000 feet	2,000	Yes	30	181	217	186
12,000 feet	2,000	No	30	202	262	227
25,000 feet	2,000	Yes	30	193	229	199
25,000 feet	2,000	No	30	218	281	244
40,000 feet	2,000	Yes	30	206	242	208
40,000 feet	2,000	No	30	233	296	259

Altitude Performance

Load tests have been made at sea level on a two-kilovolt-ampere 400-cycle high-temperature transformer to determine the extent of heating at various altitudes. To produce the same heating at other altitudes as obtained at sea level, the load was then changed. The transformer is almost always mounted on a Dural plate or channel in the aircraft structure which will dissipate considerable heat. A comparison was made on transformer heating by making load tests with the test unit mounted on a 12-inch square Dural plate $^3/_{32}$ inch thick. The unit was then freely suspended in air to omit the plate. Heating data are tabulated in Table I.

Test data tabulated in Table II show that the core dissipates most of the total losses as indicated by constant core temperature and the reduction in coil temperature with reduced loads. It may be observed that for constant ambient the allowable output at 40,000 feet is 79 per cent of normal sea-level capacity. However, since the lower ambient temperatures encountered at high altitudes will compensate for additional heating, it is safe to operate the transformer at full capacity at all times.

Table II. Output Loads at Various Altitudes for Same Core Temperatures

Altitude	Load in Watts	Ambient Temperature (Deg C)	Ultimate Temperature (Deg. C.) Outer Coil	Top of Core
Sea level	2,000	30	172	174
12,000 feet	1,830	30	156	174
25,000 feet	1,680	30	148	170
40,000 feet	1,570	30	144	170

Test data tabulated in Table III show comparative temperature rises of core and coils over an ambient temperature of minus 40 degrees centigrade. By comparing these data with the data from Table I (with Dural mounting plate) it may be observed that cold ambients are not so effective as warmer ambients on heat dissipation at the higher altitudes.

The effects of an air blast on heat dissipation are observed in Table IV. A velocity of air, measured to be 3,380 feet per minute, was forced over the unit for the purpose of obtaining, first, the decrease in heating at rated load of 2,000 watts and, second, the allowable increase in load to obtain the same heating as is encountered

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Tablle II. Output Loads at Various Altitudes and Low Ambient Temperature

Altitude	Load in Watts	Ambient Temperature (Deg C)	Ultimate Temperature (Deg C) Outer Coil	Ultimate Temperature (Deg C) Inner Coil	Ultimate Temperature (Deg C) Top of Core
Sea level	2,000	−40	99	146	108
12,000 feet	2,000	−40	123	164	127
40,000 feet	2,000	−40	171	214	171

at sea level with no forced ventilation and with use of the Dural plate.

Regulation, efficiency, and exciting current data are tabulated in Table V.

Standard Line for General Purposes

A standard line of single-phase high-temperature lightweight transformers for general purpose aircraft applications has been established. The kilovolt-ampere ratings are: 0.100, 0.250, 0.500, 1.000, 2.000, and 5.000. The voltage ratings are 120 volts high voltage and 28.5 volts low voltage. Frequencies of 380 cycles minimum to 420 cycles maximum are standard, although use of higher frequencies is not objectionable. An operating temperature rise of 200 degrees centigrade is normal and makes possible large reductions in size and weight when compared with transformers constructed with conventional materials.

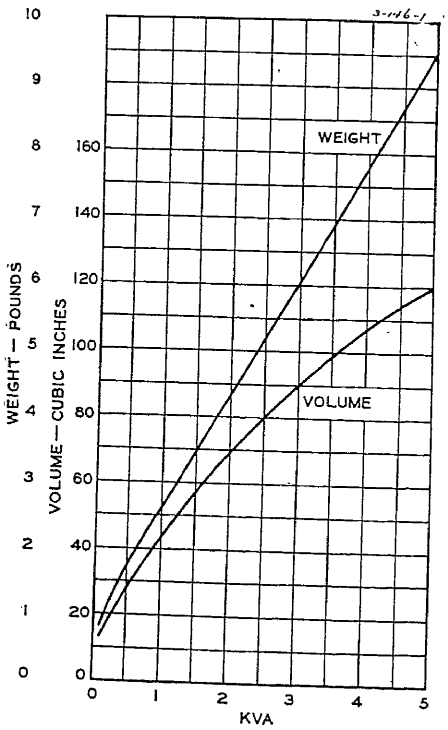

Figure 1. Weight and volume characteristics of high-temperature aircraft transformers

Table IV. Effects of Air Blast on Kilovolt-Ampere Rating at Various Altitudes

Altitude	Static Pressure Inches of Water	Load in Watts	Ultimate Temperature (Deg C) of Inner Coil
25,000 feet	0 27	2,000	117
40,000 feet	0.14	2,000	153
25,000 feet	0.27	2,670	212
40,000 feet	0 14	2,300	214

Weights and volumes are shown in Figure 1. Construction and appearance may be observed from Figure 2.

When selecting mounting location for these transformers, consideration should be given to the high temperature at which they operate.

Three-Phase Transformers

The special high-temperature materials used in the single-phase units just described are also adaptable to the construction of three-phase 400-cycle units. For example, a 32-kva three-phase unit was made with dimensions of 5 by 9 by 12 inches and weighing only 43 pounds. A 7.5-kva three-phase unit was made with dimensions of 2$^3/_4$ by 7 by 7 inches and weighing only 13 pounds. Three-phase units will weigh approximately 20 per cent more than single-phase units for the same kilovolt-ampere rating.

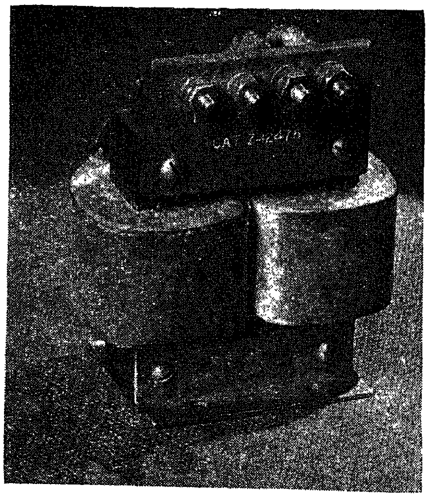

Figure 2. High-temperature aircraft transformer rated: 400 cycles, 2.0 kva, 120 to 28.5 volts

Dimensions 2$^3/_8$ by 4$^1/_4$ by 4$^5/_8$ inches

Transformer-Rectifier Units

Developmental work is well advanced on two self-regulated transformer rectifier units to operate directly from an engine-driven a-c generator; these units are rated at 100 and 200 amperes, respectively, at 30 volts, d-c output. The input frequency and voltage may vary independently of each other. The d-c output voltage may be selected at any value between 26 and 30 volts and held constant regardless of load current variations from no load to full load. The circuit includes a control reactor, a three-phase high-temperature transformer, a voltage-sensitive variable resistor, and a selenium rectifier. Units rated at 3.0 kw and 6.0 kw will weigh approximately 30 pounds and 55 pounds, respectively.

Phase-Changing Transformers

A definite trend toward increased use of single-phase electrical equipment on aircraft has been noted. Many planes, particularly large bombers, have, as standard equipment, three-phase auxiliary generators, which, of course, are of limited capacity. A single-phase load connected to any one phase of the power supply would cause considerable unbalance in the three-phase voltages, which would also reduce the available volt-ampere capacity of the generator. Therefore, it became desirable to develop a transformer to furnish a single-phase voltage and which could reflect balanced watts at equal power factors in the three generator phases. Several designs have been completed in

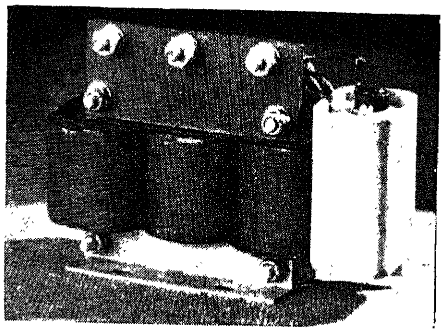

Figure 3. Three-phase to single-phase aircraft transformer rated: 400 cycles, 0.115 kva, 115 volts three-phase to 115 volts single-phase

Dimensions 2 by 3$^3/_8$ by 5$^1/_4$ inches

Table V. Electrical Data

Rating in Kilovolt-Amperes	Excitation Current in Per Cent of Full-Load Current	Voltage Regulation (Per Cent)	Efficiency (Per Cent)
0.100	42	13	87
0.250	35	11	90
0.500	26	10	91
1.000	10	7	92
2.000	7	6	93
5.000	6	5	94
32.500	5	4	95

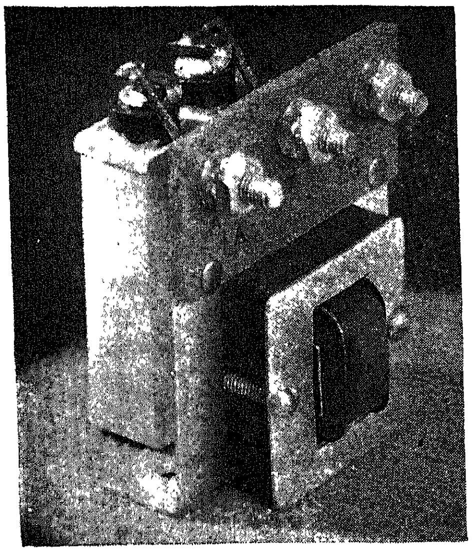

Figure 4. Single-phase to three-phase transformer rated: 400 cycles, 48 volt-amperes, 115 volts single-phase to 115 volts three-phase

Dimensions $2^1/_{16}$ by $2^3/_8$ by $2^{11}/_{16}$ inches

various ratings. Their efficiency is 90 per cent, and they operate at unity input power factor. Load variations do reflect in some unbalanced input power factor, but the output voltage regulation is not seriously affected. It is essential that phase rotation always be maintained in the stipulated direction and that the frequency remain constant. One such unit has been produced which will deliver 115 watts at 115 volts and 400 cycles from a three-phase 1,000 volt-ampere generator for the operation of a single-phase radio compass of an automatic pilot. This unit weighs 2.38 pounds and has dimensions of $2^1/_8$ by $3^7/_8$ by $5^1/_2$ inches. Refer to Figure 3 for a construction view.

For operation of small gyro instruments three-phase motors have been found most suitable. This makes it necessary on such aircraft as fighters, which are equipped with a single-phase generator, to use a single-phase to three-phase transformer for operation of these instruments. A transformer using a special nonlinear split-phase circuit is now available to supply 115 volts, three-phase, at 400 cycles from a 115-volt single-phase generator supply. This supply will operate two *KB-1* or *KC-1* General Electric gyro instruments having a total load of 48 volt-amperes. This transformer is so designed that it compensates for the difference in starting and operating load power factors and thus permits rapid acceleration of the gyro motors; however in general, this type of transformer should be designed for specific loads in which the power factor and voltamperes are essentially constant. The transformer is both small and lightweight, weighing only eight ounces. Refer to Figure 4 for a construction view.

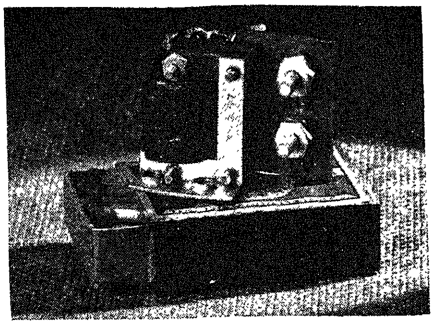

Figure 5. Instrument autotransformer rated: 400 cycles, 9.0 volt-amperes, 120 to 28.5 volts

Dimensions $1^3/_8$ by $1^3/_8$ by $1^3/_4$ inches

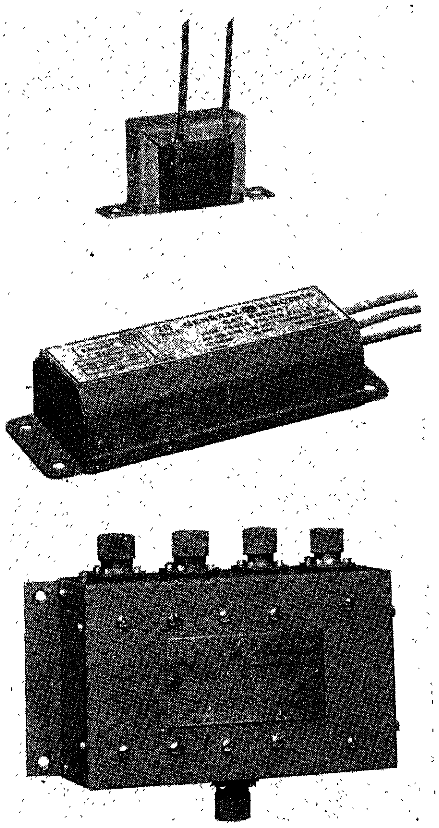

Figure 6. Reactor ballasts for fluorescent lighting on aircraft

There are other aircraft applications that require the use of two-phase to three-phase or three-phase to two-phase transformers. Such units can be made small and lightweight by use of special steels and insulating materials.

Transformers for Other Aircraft Applications

Small single-phase transformers of 4.5- and 9.0-watt ratings are available for operation of other flight instruments such as the remote indicating compass. These units weigh only two and three ounces, respectively, and are minute in physical size. Refer to Figure 5 for construction view.

Small linear a-c reactors, required for certain miniature instruments, have also been developed. One such unit, rated 74 volts, 0.03 ampere, 400 cycles, and designed for assembly within the instrument has a volume of only $1^1/_4$ cubic inches and weighs but two ounces.

Reactor ballasts are available for fluo-

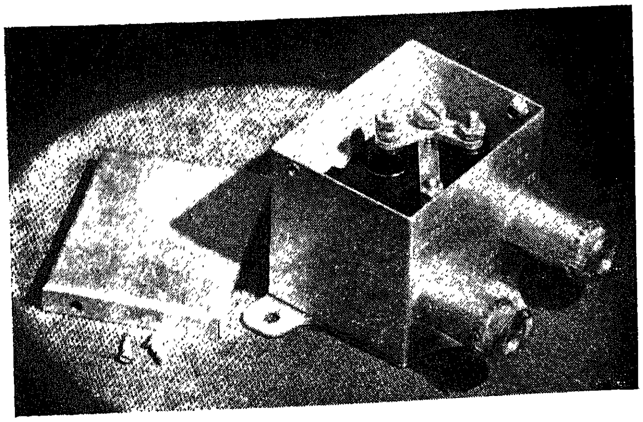

Figure 7A (left). Low-voltage ignition booster coil

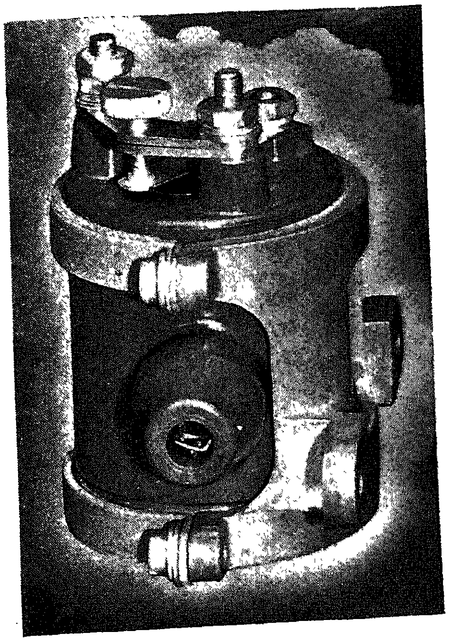

Figure 7B (right). High-voltage ignition booster coil rescent lighting in aircraf having a suitable a-c power system. They serve the same purpose as in commercial practice except that they are designed for light weight and small size and 400 cycles. Refer to Figure 6 for typical units.

Voltage-Stabilizer Transformer

A recent development in voltage stabilizers for 60-cycle service is equally adaptable for 400-cycle, or even 800-cycle, aircraft systems. These units are used for those instruments which require a constant regulated input voltage. If the frequency is constant, the load, power factor, and input voltage may vary over wide limits; if the load and its power factor are constant, the input line voltage may vary; the frequency may also vary over a ten per cent range and yet provide for a constant output voltage held to within approximately three per cent.

Booster Ignition Coils

In the past it was common practice to use high-voltage ignition booster coils for providing positive ignition in starting of aircraft engines. Recently, low-voltage booster coils have been made available, which operate directly from the battery and furnish rated current through the low-voltage winding of the magneto.

This low-voltage current is then stepped up by transformer action within the magneto to provide a high-voltage output suitable for ignition starting purposes. This system is being used extensively and is more practical in that it simplifies wiring and reduces radio interference. Refer to Figures 7A and B for typical units.

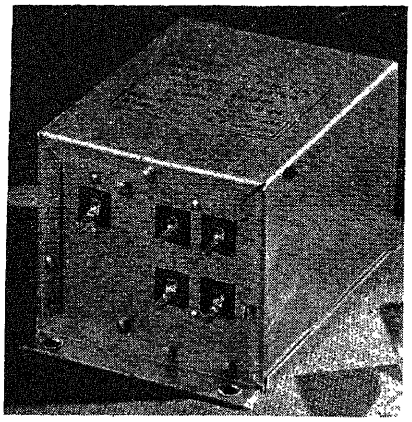

Figure 8. Vibrator-type inverter transformer rated: 24 volts d-c to 16–16 volts a-c at 60 cycles with radio inteference suppressed

Inverter Transformers

Inverter transformers are available for those applications requiring a conversion of power from d-c to a-c or requiring a d-c voltage change. The frequency of the vibrator, which is an essential part of the equipment, is limited to a preferred 120 cycles. This prevents their adoption, generally, to 400-cycle systems. They are now used extensively to provide a-c voltage to a special field winding of power-turret Amplidyne generators for controlling voltage fluctuations caused by residual flux. Without such control the turret will become unstable, thus reducing its accuracy. A small two-pound inverter transformer operating directly from the battery will provide the necessary control. Refer to Figure 8 for a typical unit.

Inverters can be made to operate directly from the variable battery voltage to provide a self-regulated a-c or d-c output voltage held constant within approximately three per cent. Wave form is almost equivalent to sine wave and entirely suitable for most applications.

A circuit[1] is usually employed in which the vibrator contacts do not break current. This provides for long reliable life of the equipment. Improvements have been made recently to correct the output wave form, increase the efficiency and provide stabilized output voltage.

Conclusions

The transformers discussed in the preceding paragraphs are those which seem most needed for aircraft applications. In many cases they have replaced heavy equipment and also have made it possible for certain generators to serve dual purposes.

There may be many other applications not covered by the foregoing paragraphs; however, it is hoped that the information disclosed herein may be of value in solving transformer problems in the aircraft industry.

Reference

1. New Type of D-C to A-C Vibrator Inverter, **O. Kiltie.** AIEE Transactions, volume 59, 1940, April section, pages 245–7.

Application of 720-Cycle Carrier to Power-Distribution Circuits

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LOW-frequency carrier has been applied extensively to distribution circuits for remote control of street lights, window lights, off-peak water heaters, and similar services. Since carrier makes available a control system whose operating flexibility can be duplicated only by the use of a number of separate wire or pilot circuits, it permits saving large amounts of copper and other critical war materials. The necessity of reducing illumination or "blacking out" lighting circuits under war conditions has emphasized the value of the direct control provided by a carrier system. More recently the importance of carrier control for air-raid sirens and for signaling air-raid warnings to defense centers and civilian-defense personnel has been demonstrated.

These types of application generally require remote control of a large number of devices from a central point on the power system and economically favor the use of a relatively low-frequency carrier which can be transmitted efficiently through power-distribution transformers in order to reach the 115- or 230-volt power supplies available for operation of the receiving controllers. The carrier frequency of 720 cycles commonly used in these applications has been selected after carefully evaluating the relative economic values of transmission efficiency, simplicity of equipment, and receiving-controller sensitivity required to prevent interference from frequencies normally present on the power system.

This paper presents methods by which the 720-cycle transmission characteristics of power-distribution systems can be predicted to ensure successful operation of the carrier channel. The applicability of these methods has been confirmed by calculations, calculating-board studies, and field measurements conducted on actual installations over a 15-year period. To simplify the application effort required, the methods utilize approximations which do not always yield precise results, but the over-all error is generally conservative.

Basically, the application problem is one of transmitting energy over a power system at 720 cycles instead of at 60 cycles, so the application engineer follows, in many ways, the same steps as with 60-cycle engineering. Even though the 720-cycle voltage used is only a few per cent of the 60-cycle system voltage, there are similar problems of voltage drop or rise, favorable location of the generator (transmitter), watts generated and watts delivered, and vars that must be supplied either at the transmitter or at other points.

Although conductor size, and spacing, and other factors are involved, the usual 60-cycle conception is that lines up to 30 miles length are considered short lines—if a solution of high accuracy is required—or up to approximately 120 miles if only fair accuracy is required. In extending this conception to 720-cycle transmission it must be remembered that short and long lines are relative terms depending on the impressed frequency. A 120-mile 60-cycle transmission line often can be considered a short line, since its distributed capacitance causes a no-load voltage rise at the receiving end of only three per cent at 60 cycles, but this same no-load voltage rise is obtained at 720 cycles if the line length is decreased in the same ratio as the frequency is increased. For this reason a ten-mile line at 720 cycles and a 120-mile line at 60 cycles are both short lines within the same limits of accuracy, and 720-cycle transmission problems can generally be analyzed with reasonable accuracy using short-line conceptions for transmission distances up to ten miles. Beyond this distance the errors introduced by applying short-line methods increase but are generally conservative (unfavorable to the application) for even greater distances. Hereafter the term "short line" is understood to include transmission over overhead lines up to ten miles distance.

Short-line applications are most easily solved when it is practical to couple the 720-cycle transmitter into the 60-cycle system so that, at both frequencies, power flows toward the loads in the same direction in all sections of the feeders. In these applications the 720-cycle performance can be predicted from values which usually have been determined previously in order to plan properly the 60-cycle operation of the system, such as voltage drop, conductor size and spacing, total load kilovolt-amperes, power factor, and the system short-circuit kilovolt-amperes. It is then unnecessary to know how the load is distributed over the system since the effect of this factor is included in the actual 60-cycle voltage drop.

In applications where data of this kind are not available, it is usually easier to determine the 720-cycle performance from factors which take into account the 60-cycle kilovolt-ampere load, the distribution of the load, and the system short-circuit kilovolt-amperes. This is also the best method for long-line applications, as well as for short-line applications in which power does *not* flow toward the load in the same direction at both 60 and 720 cycles, because, in these cases, the relation between 60- and 720-cycle voltage drops often becomes complex.

Complicated systems involving long lines or elaborate interconnections with many sources of 60-cycle power may require the use of a network analyzer or calculating board if a highly accurate solution is required.

The two schemes commonly used to couple the 720-cycle transmitter to the power system are the series and the shunt coupling arrangements. With series coupling the 720-cycle voltage is introduced in series between the load and the 60-cycle generator. With shunt coupling it is introduced in parallel with the load and 60-cycle generator. In either case the 720-cycle loss in the generator depends on the 720-cycle voltage present, the generator 60-cycle reactance (or the 60-cycle short-circuit kilovolt-amperes) and the 60-cycle kilovolt-ampere rating of the load on which the 720-cycle voltage is impressed. For a very large ratio of 60-cycle short-circuit kilovolt-amperes to load kilovolt-amperes, the physical size of the 720-cycle transmitter is smaller, using series coupling, but below some smaller value of this ratio the transmitter physical size is smaller when using shunt coupling.

On any system it is always possible to locate the transmitter or transmitters to provide the desired coverage. As examples, the control voltage can be impressed on only a portion of one primary feeder, or on one entire primary feeder, or on all primary feeders from one substation, or on a number of substations fed

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from a common higher-voltage transmission system. In each of these examples different values of 60-cycle load and short-circuit kilovolt-amperes are encountered, which in turn influence the size of the transmitter and the choice between series and shunt coupling.

720-Cycle Characteristics of 60-Cycle Lines and Apparatus

When 720 cycles is superimposed on a 60-cycle circuit, for purposes of calculation, all values which are affected by frequency must be corrected as follows:

1. The reactance of the feeder conductors and the leakage reactance of transformers are proportional to frequency and hence are increased in the ratio of 720/60 or 12 times. But the series resistance of the feeder conductors and transformers increases relatively little with frequency, and therefore the series resistance can be safely neglected at 720 cycles for determining current flow or voltage drop.

2. Distribution-transformer susceptance, which determines the transformer exciting current, is directly proportional to frequency and, hence, can be safely neglected.

3. A distribution transformer can be represented by the transformer leakage reactance in series with the secondary load. This reactance is increased 12 times by the frequency increase. The transformer voltage drop is also increased, being of the order of ten per cent at 720 cycles compared to two or three per cent at 60 cycles.

4. To determine the characteristic of the 60-cycle load, it is convenient to divide the load into the two following types:

(*a*). Noninductive loads such as lights, ranges, water heaters, and heating appliances whose 720-cycle impedance can be represented by a pure resistance numerically equal to the 60-cycle load impedance. Hereafter these are classed together as lighting load.

(*b*). Inductive loads composed of motors are treated as though the motors stand still since there is no transfer of mechanical power through the motor shaft. Thus the locked-rotor reactance determines its 720-cycle impedance. The 60-cycle locked-rotor reactance of a typical three-phase motor is 20 per cent. The 720-cycle reactance is increased 12 times and is equal to 240 per cent of the motor full-load 60-cycle impedance. Single-phase motors show a similar 720-cycle impedance increase. The amount of motor load present can be determined in order to obtain an accurate solution, but this usually does not justify the additional complexity of the solution. The usual practice is to consider the entire load including the motors as lighting load. This approximation is on the conservative side, since the smaller load impedance used indicates somewhat greater than actual 720-cycle voltage drop and power loss.

5. The capacitive reactance attributable to conductor capacitance or power-factor-correction capacitors is inversely proportional to frequency and hence is divided by 12 at 720 cycles. A small amount of capacitance present corrects the system power factor and decreases the 720-cycle voltage drop. Excessive amounts of lumped capacitance have the opposite effect, but, if undesirable effects do occur, they can always be overcome by applying corrective measures at the capacitors. The short-line methods outlined hereafter neglect the effect of distributed capacitance, and the effect of lumped capacitance is treated separately.

Base Used for 720-Cycle Voltage Calculations

For convenience the 720-cycle voltage at all points on the system is referred to a 115-volt 60-cycle base. Hence, the 720-cycle voltage at any point is the voltage at that point on a 115-volt 60-cycle base multiplied by the transformation ratio of the transformer (or transformers) intervening between this point and a 115-volt 60-cycle circuit. The voltages considered on a 115-volt 60-cycle base are directly applicable to 115-volt receiving controllers. The 230-volt controllers required operating voltages exactly twice those for 115-volt controllers. But in practice a 230-volt controller receives exactly twice the voltage received by a 115-volt controller at the same location, so the results are equally applicable if controllers of either or both voltage ratings are actually applied.

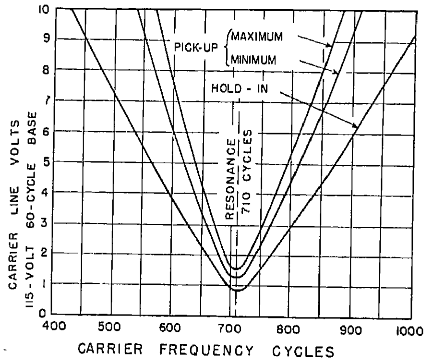


Figure 1. Frequency-voltage characteristics of 720-cycle carrier relay as used in General Electric types *SCS*-15 and *SCS*-17 controllers.

Permissible Range in 720-Cycle Received Controller Voltage

The frequency-voltage characteristics of the 720-cycle controllers are shown in Figure 1. The 710-cycle operating voltage range is 1.5 to 15 volts on a 115-volt 60-cycle base. Because of motor slip in the 60/720-cycle conversion at the transmitter, the transmitted frequency usually ranges from 713 to 716 cycles under different load conditions, with a total variation from 707 to 723 cycles if the standard allowance of 60 cycles ± $^1/_2$ cycle is made for transmitter-power-supply variation. For this complete frequency range, the recommended controller voltage range is 1.75 to 15 volts. If sequence coupling is used so that a controller must not operate on one feeder when another feeder is energized, the maximum recommended interfering voltage is one volt.

720-Cycle Power Consumption by Controller

The 720-cycle power consumption of a controller is approximately the same as the 720-cycle power absorbed by a 60-cycle load of one kilovolt-ampere, so it usually represents a small per cent of the total 720-cycle power. In view of other unfavorable assumptions the controller power consumption is ignored unless it exceeds 20 per cent of that taken by the feeder 60-cycle load. Beyond this point the effect of the controllers should be included by adding one kilovolt-ampere of 60-cycle load for each additional controller.

Short-Line Voltage-Drop Equations

Equations 1 and 2 are well known and entirely acceptable in determining 60-cycle voltage drop, but for reasons outlined below, equation 3 is preferable for use at 720 cycles.

Per cent volts drop

$$=\frac{(\text{kva})d\ (R\cos\theta+X\sin\theta)}{10(\text{kv})^2} \qquad (1)$$

where

kva = load in three-phase kilovolt-amperes
d = length of line in miles
θ = load power-factor angle
R = line resistance in ohms per mile
X = line reactance in ohms per mile
kv = line kilovolts

An equation for 60-cycle voltage drop can also be written as:

Per cent volts drop

$$=\%IR\cos\theta+\%IX\sin\theta \qquad (2)$$

where

$\%IR=IR$ volts expressed as a percentage of the received volts
$\%IX=IX$ volts expressed as a percentage of the received volts

If a 720-cycle voltage is superimposed so that power flows toward the load in the same direction at both 60 and 720 cycles and if the assumptions in the preceding paragraphs numbered one to five apply, then the current through each load per volt of impressed load voltage is the same at both frequencies. The $\%IX$ is directly proportional to frequency. The $\%IR$ is the same at both frequencies, but, in view of the assumption made in paragraph 1, the 720-cycle value of R or $\%IR$ is zero. Also θ is 0 under the assumption

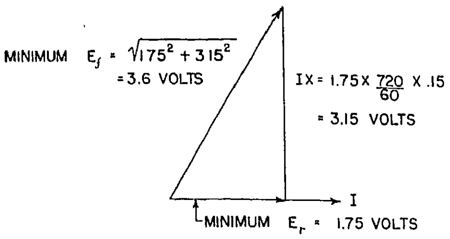

MINIMUM $E_f = \sqrt{1.75^2 + 3.15^2}$
= 3.6 VOLTS
$IX = 1.75 \times \frac{720}{60} \times .15$
= 3.15 VOLTS
I
MINIMUM E_r = 1.75 VOLTS

Figure 2. Feeder input volts required when the 60-cycle %*IX* between the controller and the input point is 15 per cent

in paragraph 4*b*. It can now be seen that the equations 1 and 2 are based on approximations which are not valid at 720 cycles because, if R or $\%IR$ and θ are 0, the per cent volts drop is 0.

By solution of Figure 2 on a per-unit basis, equation 3 is obtained which is exact within the limits of the assumptions previously made.

$$E_f = E_r\sqrt{1+(0.12\times\%IX)^2} \tag{3}$$

where

E_f = feeder 720-cycle transmitted volts on 115-volt 60-cycle base

E_r = 720-cycle volts received at 115-volt controller

$\%IX$ = total 60-cycle IX volts from transmitting end of feeder to the controller expressed as a percentage of the 60-cycle volts at the receiving controller

To determine accurately the total 60-cycle $\%IX$ it is often necessary to divide a primary feeder into a number of sections for which the 60-cycle load or current is constant for each section. The 60-cycle $\%IX$ can then be determined for each section. The $\%IX$ to the end of the feeder is the sum of the $\%IX$ for each section traversed in reaching the end of the feeder. Adding the distribution transformer and secondary feeder $\%IX$ gives the total $\%IX$ to reach the controller.

Feeder 720-Cycle Input Volts Required

Substituting in equation 3 the minimum required value of E_r, and for $\%IX$ a value applying to the last controller (or customer) on the last feeder distribution transformer gives the minimum E_f necessary to provide control at all points on the feeder. If the 60-cycle $\%IX$ for the last controller is 15 per cent, the 720-cycle voltage relations are as shown in Figure 2. Figure 3 shows the minimum E_f required for other values of 60-cycle $\%IX$.

E_f is easier to calculate than is the actual feeder 60-cycle voltage drop. For the usual overhead feeder circuits, X does not vary much with conductor size or spacing. Thus E_f depends chiefly on the feeder 60-cycle characteristics of load and distance, while the 60-cycle voltage drop depends chiefly on load, distance, conductor size, and load power factor.

Figure 4 shows values of 60-cycle $\%IX$ for one per cent 60-cycle volts drop and for various 60-cycle load power factors and values of line X/R. Since average feeder load power factors are well below 80 per cent, the curves show that $\%IX$ changes only slightly for a given X/R for power factors encountered. Values of X and R are given in standard-wire tables.[1] Values of X/R range from approximately 0.35 to 2.5 for the usual overhead feeder conductor spacings and for copper-wire sizes from number 6 to 0000, respectively. For this range of X/R and a 50 to 80 per cent power-factor range, Figure 4 shows that $\%IX$ varies from 0.35 to 1.08. For the largest wire size 0000 and 70 per cent power factor, the 60-cycle $\%IX$ is approximately equal to 60-cycle per cent volts drop.

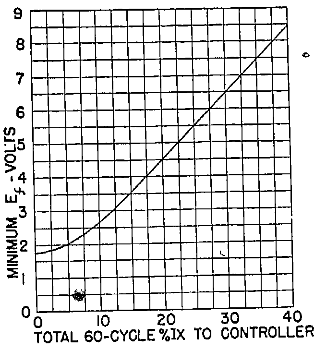


Figure 3. Feeder input volts as function of the 60-cycle %*IX* between the controller and the input point

In view of the preceding, if data are available on the 60-cycle voltage drop, Figure 4 is used to determine the total $\%IX$ and thereby the required feeder 720-cycle volts from Figure 3.

If the 60-cycle drop is unknown, it usually can be estimated based on the maximum voltage drop that can be tolerated without getting unsatisfactory 60-cycle voltage at the last customer. Power-factor correction capacitors should be disconnected and voltage regulator blocked in determining the 60-cycle voltage drop.

Even though different wire sizes are used on different sections of the feeder, it is usually possible to estimate an average value of X/R which is weighted for the relative amount of voltage drop in each section. The value obtained from Figure 4 (for this average value of X/R), when multiplied by the total 60-cycle per cent volts drop then gives the total 60-cycle $\%IX$. If the average value of X/R taken corresponds to the largest value for any section of the feeder, the solution becomes very simple, although in some cases this assumption may lead to the application of larger 720-cycle transmitter equipment than is actually required.

If data are not available on 60-cycle voltage drop, the 60-cycle $\%IX$ can be calculated for each section of the feeders using equation 4.

$$\text{Feeder 60-cycle } \%IX = \frac{(\text{kva})\ dX}{10(\text{kv})^2} \tag{4}$$

Feeder 720-Cycle Power Input Required

For feeders previously defined as short lines the feeder 720-cycle kilowatt and kilovolt-ampere input depends on the following factors:

1. The 720-cycle-feeder volts input.
2. The 60-cycle-feeder load.
3. The 60-cycle $\%IX$ for each section of the feeder.

The curves of Figure 5 are based on five volts input on a 115-volt 60-cycle base. This voltage is more than is required for most feeders but has been used because the standardized series-coupling arrangements provide five volts in series with the feeder. The actual feeder voltage varies from five volts to some lower value which depends on the application conditions. Since power varies as the square of the voltage, each value from Figure 5 can be corrected for any other voltage.

If the same 720-cycle voltage (on a 115-volt 60-cycle base) is impressed on feeders having the same 60-cycle per cent reactance, the same 720-cycle power input

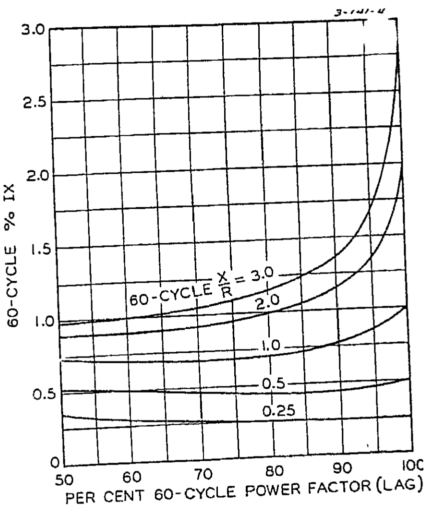


Figure 4. %*IX* based on one per cent 60-cycle volts drop as function of load power factor and feeder *X/R* ratio

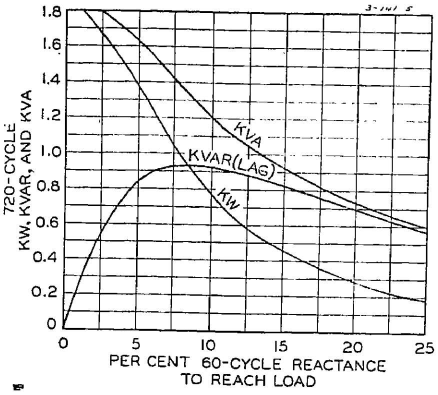


Figure 5. The 720-cycle power input with five volts (on a 115-volt 60-cycle base) impressed on a feeder with 1,000-kva concentrated 60-cycle lighting load

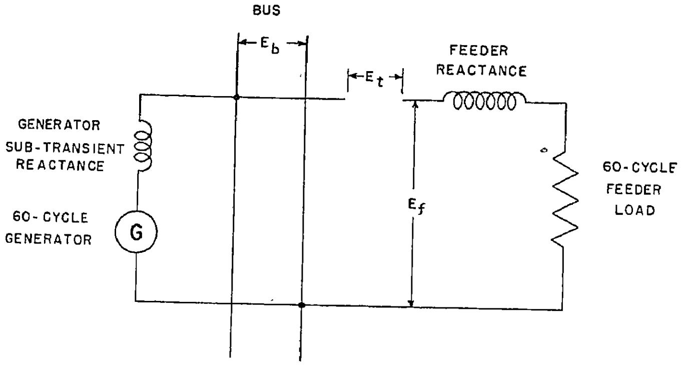


Figure 6. The 720-cycle voltages superimposed on a 60-cycle system using series coupling

Shown single phase for simplicity

per 1,000 kva of 60-cycle load is required for each feeder. Values obtained from Figure 5 can be prorated in direct proportion to 60-cycle kilovolt-ampere rating to cover larger or smaller systems.

As explained later values from Figure 5 do not include additional power losses occurring in the power system because of 720-cycle current flow through the generators and through irrelevant loads which usually parallel the generators. Also allowance must be made for losses in the 720-cycle coupling apparatus, which are usually made fairly high in the interest of stable operation and reasonable first cost.

The 60-cycle per cent reactance used in Figure 5 can be determined readily if all the load is concentrated at one point on the feeder. On most distribution feeders the load is distributed, and it is necessary to determine the per cent reactance for an equivalent concentrated load circuit. A fair approximation is to move all the load to a point on the feeder where the same 60-cycle power loss is obtained. Frequently, there is no load up to a certain point, and beyond this point the load is uniformly distributed. The same 60-cycle power loss is then obtained if all the load is concentrated at a point one-third the distance from the point to the end of the feeder. For this case the per cent reactance can be obtained from the 60-cycle power factor and 60-cycle voltage drop data using equation 5.

Equivalent 60-cycle per cent reactance

$$=\%IX[V_{d1}+{}^1/_3(V_{d2}-V_{d1})] \qquad (5)$$

$$=\%IX({}^2/_3V_{d1}+{}^1/_3V_{d2})$$

where

$\%IX$ = value of 60-cycle $\%IX$ (from Figure 4)

V_{d1} = 60-cycle per cent volts drop to the first customer on the feeder

V_{d2} = 60-cycle per cent volts drop to the last customer on the feeder

When the 720-cycle voltage is simultaneously impressed on two or more feeders the 60-cycle feeder load kilovolt-amperes used to enter Figure 5 is the sum of the loads on all these feeders and the per cent reactance for the equivalent concentrated load circuit is also expressed on this base.

Distribution-Feeder Characteristics Based on Operating Experience

Since the 60-cycle characteristics of feeders are subject to wide variations, it is evident that the 720-cycle feeder characteristics are also subject to variations. However, operating experience on a large number of installations indicates that the 720-cycle variations are not as wide as might be expected from the aforementioned considerations. In many applications the 720-cycle energy is superimposed so that it flows toward the load in the same direction as the 60-cycle power. Also only one transformation is usually involved to reach any controller, and all of the assumptions in the preceding paragraphs numbered 1 to 5 apply with negligible exceptions. Thus operating experience leads to the following conclusions:

1. A 720-cycle voltage of four volts (on a 115-volt 60-cycle base) is adequate to provide complete control at all points on any feeder.

2. A 720-cycle voltage of as little as three volts is adequate if the system characteristics are favorable or if control is only required at favorable points such as at points near the substation or near the end of the primary feeder but not at the ends of the secondary circuits.

3. For determining feeder 720-cycle power input under peak-load conditions as previously described it is usually conservative to consider the total load concentrated in series with a 60-cycle reactance of ten per cent on the feeder load base.

Schematic Diagrams for Series and Shunt Coupling

The essential differences between the series- and shunt-coupling arrangements are shown in Figures 6 and 7. It is understood that generator reactances shown include reactance in the intervening transmission system if the 60-cycle generator is remote from the point at which the 720-cycle coupling is made.

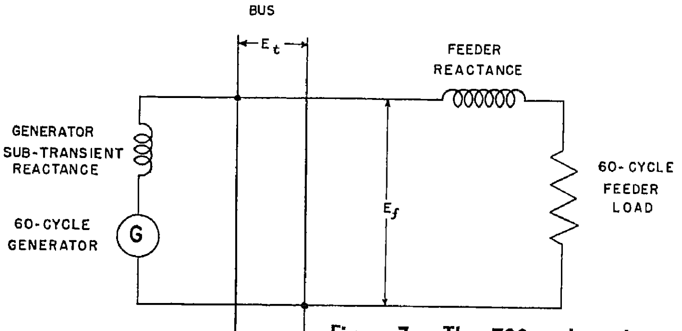


Figure 7. The 720-cycle voltages superimposed on a 60-cycle system using shunt coupling

Both series- and shunt-coupling arrangements include circuits (not shown) which prevent interference with the normal flow of 60-cycle current and prevent appreciable flow of 60-cycle current through the source of 720-cycle voltage. The 720-cycle transmitted voltage E_t is shown as being introduced directly on the 60-cycle system to simplify these pictorial explanations of the 720-cycle operation.

Theory of Operation of Series-Coupling Arrangement

In the series arrangement of Figure 6, the 720-cycle voltage E_t is introduced in series with the load on the feeder over which control is required. The 720-cycle current flows through the complete series circuit as illustrated. The relations between the voltage are:

$$E_f=E_t-E_b \qquad (6)$$

$$E_b=E_t-E_f \qquad (7)$$

The 720-cycle impedance of a feeder of given 60-cycle voltage and per cent re-

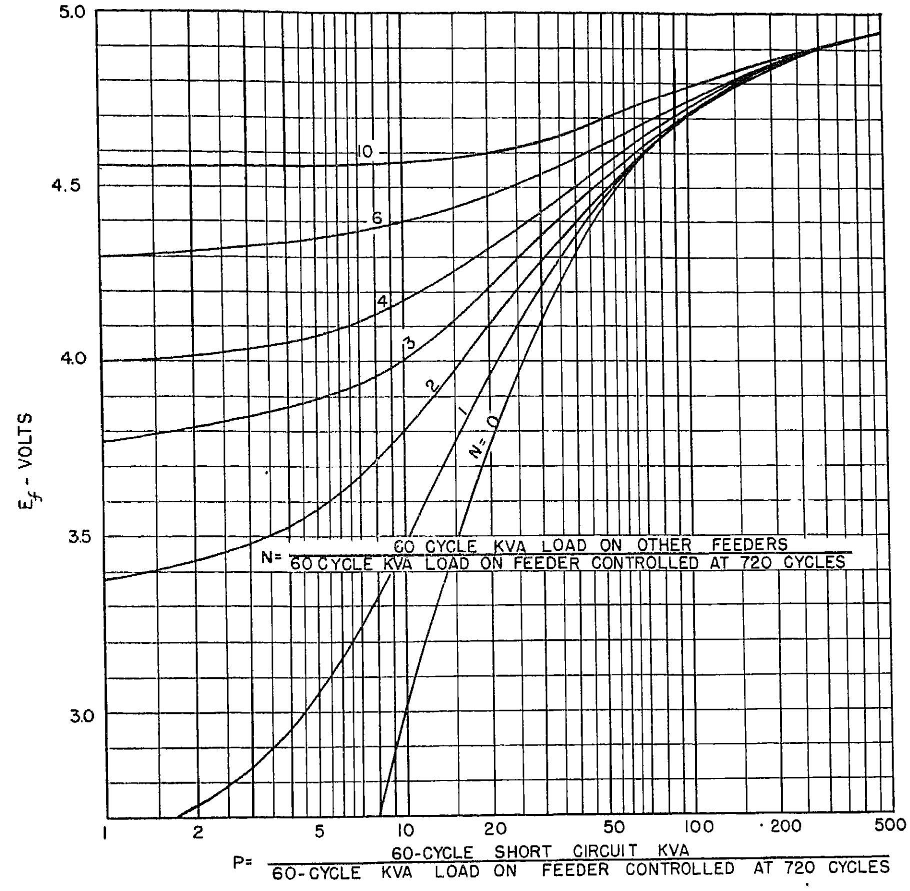


Figure 8. Feeder input volts obtained with five volts in series with a feeder for 60-cycle values of load on the feeder, load on other feeders, and short-circuit capacity at the coupling location

Each feeder has concentrated lighting load and ten per cent reactance on its own base

actance is inversely proportional to the feeder 60-cycle load kilovolt-amperes. The generator 720-cycle impedance depends almost entirely on the generator 60-cycle subtransient reactance which also determines the generator 60-cycle rms symmetrical short-circuit kilovolt-amperes. Therefore the generator 720-cycle impedance is inversely proportional to the generator 60-cycle short-circuit kilovolt-amperes. Hence, it follows that, if the 60-cycle ratio of generator short-circuit kilovolt-amperes to feeder load kilovolt-amperes is large, which is usually the case, the series coupling arrangement is highly efficient.

Values of E_f, when E_t equals five volts, are plotted in Figure 8. If carrier is coupled simultaneously to all feeders on the bus, as in Figure 6, the load on other feeders is zero, and the curve for $N=0$ gives the applicable values of E_f. It is seen that if the short-circuit kilovolt-amperes is at least 26 times the load on the feeder over which control is required, the feeder 720-cycle voltage is four volts or more. When the bus is fed by a single generator (or transformer bank) the minimum value of E_f is obtained under feeder peak-load conditions since the lowest ratio of short-circuit kilovolt-amperes to load kilovolt-amperes is then obtained. If the same bus is fed by two or more generators and some may be out of service during light loads, the ratio of generator short-circuit kilovolt-amperes to feeder load kilovolt-amperes is least for the operating condition under which the generators in service carry the maximum 60-cycle load in per cent of their rating. Since the generators usually carry the highest per cent load under bus peak-load conditions the minimum value of E_f is usually obtained during station peak-load conditions but in exceptional cases may occur under light-load conditions.

When other feeder loads are connected across the bus, the value of E_f is increased because this load is in parallel with the generator reactance, and the resultant 720-cycle impedance of the bus is decreased. Figure 9 shows Figure 6 modified to include other feeders on the same bus. In Figure 8, E_f is plotted for values of N from 1 to 10 applicable to the circuit of Figure 9. To visualize the significance of these curves, it is convenient to consider that all feeders have the same 60-cycle load, since N is then equal to the number of the other feeders on the bus. Note that if P is constant, E_f increases as N increases or as more feeders are added. In practice, P usually increases as N increases because larger generator (or station transformer) capacity is required to carry the increased load. With $N=4$ (that is, a total of five equally loaded feeders) a value of four volts is obtained for E_f even if the short-circuit kilovolt-amperes is zero.

It is fairly typical in substation layouts to have four feeders of equal kilovolt-amperes rating present. For this case $N=3$; however, if it is assumed that the maximum load on the feeder over which control is required is 50 per cent greater than the average load on the other feeders, a value of $N=2$ is obtained. From Figure 8 it is seen that for $N=2$ and $E_f=4$, the 60-cycle short-circuit kilovolt-amperes must be 16 times the kilovolt-ampere load on the most heavily loaded feeder. If the short-circuit kilovolt-amperes is higher than this, E_f is higher, and more efficient operation is obtained. The minimum value of E_f for Figure 9 is almost always obtained during the station peak load.

Values of E_f obtained from Figure 8 should at least equal the 720-cycle input volts previously determined necessary to provide adequate 720-cycle control over the actual feeders.

The arrangement of Figure 9 is used frequently for sequence coupling in which carrier is transmitted in sequence through coupling transformers in each of two or more feeders. The 720-cycle voltage is then impressed in succession on each feeder or group of feeders. In this case the maximum value of E_b in Figure 9

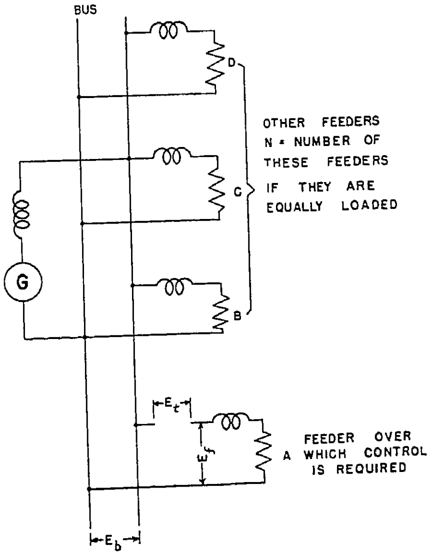

BUS
D
OTHER FEEDERS
N = NUMBER OF THESE FEEDERS IF THEY ARE EQUALLY LOADED
C
G
B
E_t
E_f
A
FEEDER OVER WHICH CONTROL IS REQUIRED
E_b

Figure 9. The 720-cycle voltages superimposed on a 60-cycle system using series coupling to obtain independent control over one of several feeders

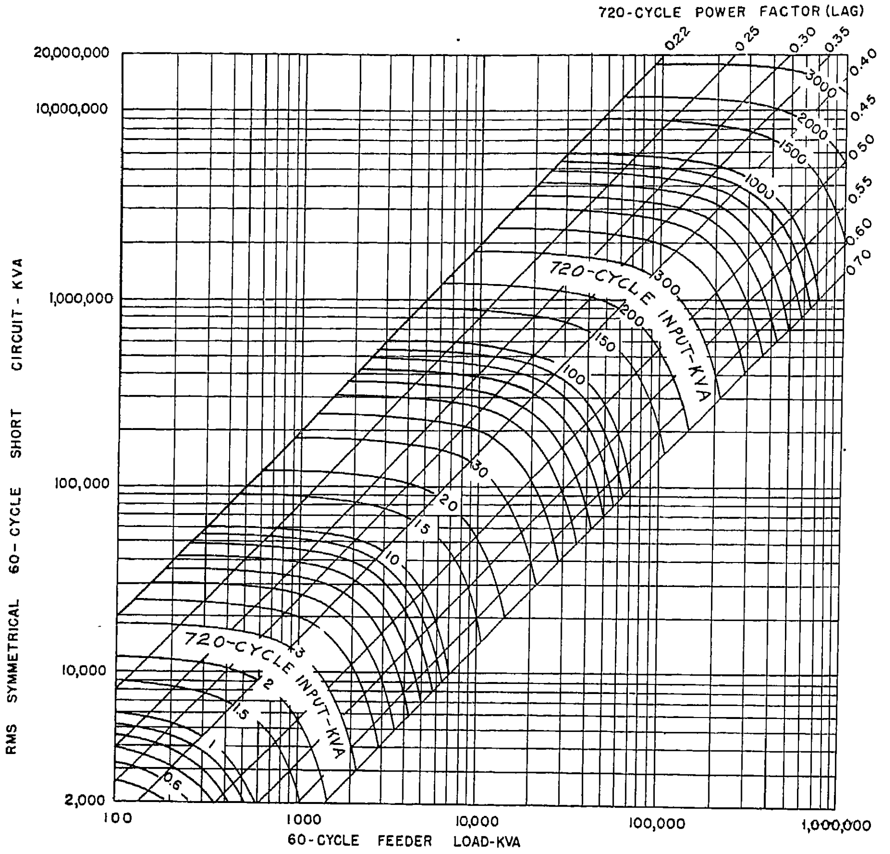


Figure 10. The 720-cycle input kilovolt-amperes and power factor using shunt coupling to superimpose five volts on the system of Figure 11

should not exceed one volt in order to use sequence coupling safely. Thus, using the customary transmitted 720-cycle voltage of five volts, values of E_f obtained from Figure 8 should be four volts or more under all substation operating conditions. Otherwise, a suitable 720-cycle shunt is required across the bus to decrease E_b to the required minimum value.

Note that if a tie exists between feeders *A* and *B* in Figure 9 or between their secondary loads, circulating current in the tie may prevent the efficient application of sequence coupling to feeders *A* and *B* individually. However, by energizing the coupling transformers on both feeders simultaneously at 720 cycles, equal and in-phase voltages E_t are introduced in-series with both feeders simultaneously so that simultaneous control is provided over both feeders even when they are tied together. On a given system this arrangement tends to decrease E_f and to increase E_b obtained from Figure 8, because the load on the feeder controlled at 720 cycles becomes the sum of the load on feeders *A* and *B*, and this decreases the value of *P*.

If the resulting operating flexibility cannot justify the operating time required to operate controllers on all feeders when using feeder sequence coupling as shown in Figure 9, it is feasible to impress 720-cycle voltage on a group of two or more feeders simultaneously. In order to obtain maximum E_f and minimum E_b it is then preferable to make the phase of E_t different for each feeder in each group as follows:

A. If two radial feeders are controlled simultaneously, through a simple connection of the coupling transformers the voltage is introduced 180 degrees out of phase on each feeder so that, if the feeder loads are identical, no 720-cycle current flows through the bus, and hence E_b is zero, and E_f is equal to E_t.

B. If three radial feeders are controlled simultaneously, through a simple connection of the coupling transformers equal voltages E_t can be introduced 120 degrees out of phase in each feeder so that, if the feeder loads are identical, no 720-cycle current flows through the bus. In practice the feeder loads may not be identical, but, assuming the maximum possible unbalance, the values of E_f and E_b will not restrict the operation any more than the corresponding limiting values obtained by using individual feeder sequence coupling (that is, energizing the feeders one at a time at 720 cycles).

Here again values of E_f should be compared with those previously determined necessary to provide control over the actual feeder or feeders. If more voltage is required, it can be provided with either of two methods. A 720-cycle shunt can be connected across the feeder bus to decrease the bus impedance at 720 cycles, or coupling transformers capable of providing the necessary 720-cycle voltage can be selected. The choice between these methods is one of economics unless the inevitable increase in the interfering voltage E_b causes interference between controllers on feeders provided with sequence coupling. If so, the former method is mandatory, if the advantages of sequence coupling are retained.

Theory of Operation of Shunt-Coupling Arrangement

In the shunt-coupling arrangement of Figure 7, E_t is impressed across the bus and the voltage across the feeder E_f is equal to the voltage E_t. The voltage E_t is also impressed directly across the 60-cycle generator. The 720-cycle power input to the feeder was determined previously (see Figure 5). The generator 720-cycle kilovolt-ampere input is directly proportional to the generator rms symmetrical 60-cycle short-circuit kilovolt-amperes. Tests show that the 720-cycle power factor of 60-cycle generators is approximately 15 to 20 per cent. Hence if the generators are directly connected to the bus, approximately 20 per cent of the 720-cycle kilovolt-ampere input to the generators represents actual kilowatts.

Using the shunt-coupling arrangement under the conditions assumed for Figures 10 and 11, the total 720-cycle input kilovolt-amperes and power factor can be determined directly if values of 60-cycle short-circuit kilovolt-amperes and feeder load kilovolt-amperes are known. Since carrier control is usually required under all load conditions, the 720-cycle input is usually determined for a feeder load based on peak-load conditions and for a short-circuit capacity based on all generators in service under peak-load conditions, making a reasonable allowance for future increases in these values because of system growth.

Values obtained from Figure 10 are based on five volts, 720 cycles on a 115-volt 60-cycle base but may be corrected for any other 720-cycle voltage, since the 720-cycle power factor is constant and the

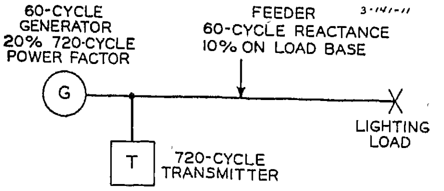

60-CYCLE GENERATOR 20% 720-CYCLE POWER FACTOR
G
FEEDER 60-CYCLE REACTANCE 10% ON LOAD BASE
3-141-11
LIGHTING LOAD
T
720-CYCLE TRANSMITTER

Figure 11. Diagram on which Figure 10 is based

720-cycle kilovolt-amperes varies directly as the square of the voltage.

If the generators are located at a remote point on the system, the 720-cycle kilovolt-ampere input toward the generators is still limited by the same reactance which limits 60-cycle short-circuit kilovolt-amperes, and reasonably accurate values can be obtained from Figure 10, providing 720-cycle standing-wave effects on the transmission line (or lines) to the generators can be neglected. When the transmission-line reactance has a negligible effect on the 60-cycle short-circuit kilovolt-amperes the error attributable to neglecting standing-wave effects can be ignored. Even if the transmission-line reactance is not negligible in determining 60-cycle short-circuit kilovolt-amperes, reasonable accuracy can be obtained neglecting standing-wave effects for line distances to the generators up to 32 miles (corresponding to one-eighth wave length at 720 cycles). For line distances in excess of 32 miles the values obtained from Figure 10 are subject to errors which vary with the system characteristics.

The two tuning schemes used with shunt-coupled transmitters are usually designated as constant-voltage tuning and maximum-current tuning. To use the coupling equipment for protection against interference from a neighboring interconnected 720-cycle transmitter, constant-voltage tuning must be used. (See "Interference Between Two or More Interconnected Transmitters on the Same Power System.") Otherwise, the tuning scheme requiring the least 720-cycle kilovolt-amperes rating is used. This is usually the maximum-current tuning scheme.

Constant-Voltage Tuning Scheme

If this scheme is used, the 720-cycle power source (converter) and coupling circuit are tuned so that no over-all reactive voltage drop occurs between the point at which the 720-cycle voltage is generated in the converter and the 60-cycle bus of the system. The 720-cycle voltage on the 60-cycle bus is essentially independent of the system 60-cycle values of feeder load kilovolt-amperes or short-circuit kilovolt-amperes. The 720-cycle converter furnishes the 720-cycle kilovolt-ampere input to the 60-cycle system plus the kilowatts coupling loss.

Maximum-Current Tuning Scheme

If this scheme is used, the converter and coupling circuit are tuned for maximum 720-cycle current into the 60-cycle bus when the 60-cycle values of feeder load and short-circuit kilovolt-amperes are a maximum.

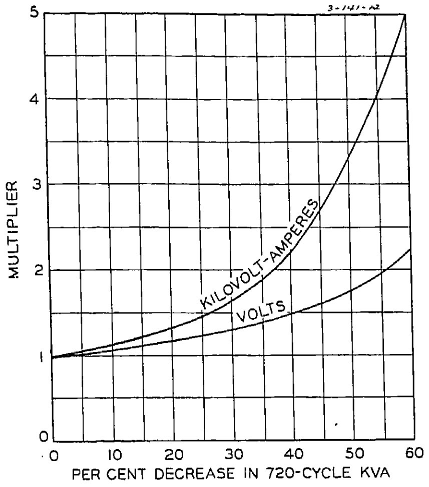


Figure 12. 720-cycle multipliers used for maximum-current shunt-coupling arrangement when system-switching conditions decrease the short-circuit capacity during peak-load conditions

When tuned, the converter furnishes the 720-cycle kilowatts input to the 60-cycle system plus the kilowatts coupling circuit loss. The 720-cycle reactive kilovolt-amperes required by the 60-cycle system is neutralized by a reactive kilovolt-amperes of opposite phase provided by the coupling circuit. With this scheme the over-all transmitter circuit becomes detuned under light 60-cycle load conditions, and the 720-cycle volts transmitted decreases. However, since the transmitter output voltage required to operate the controllers under light-load conditions drops more than the decrease in 720-cycle volts transmitted, limiting conditions are still obtained at peak load. But if the 60-cycle generator or transmission-line switching changes during peak-load conditions, the 720-cycle transmitted voltage drops because of detuning, and it is necessary to multiply values of 720-cycle kilovolt-amperes and volts by the multipliers obtained from Figure 12.

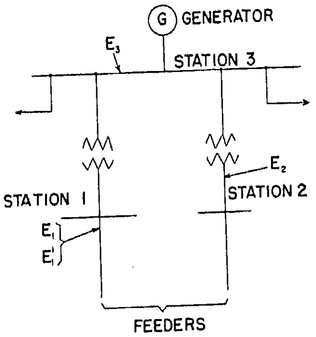


Figure 13. Voltages introduced on power system by transmitters at two stations

Interference Between Two or More 720-Cycle Transmitters on the Same Power System

Assume that transmitters are applied to provide independent control over the two feeders out of stations 1 and 2 in Figure 13. When transmitting separately station 1 and 2 transmitters impress voltages of E_1 and E_2, respectively, as shown. E_1', the interfering voltage on station 1 feeder produced by station 2 transmitter on station 1 feeder, should not exceed one volt; otherwise, incorrect operation may result.

If both transmitters are operated simultaneously and synchronized in phase, the total voltage on the station 1 feeder is E_1+E_1'. If both transmitters are synchronized 180 degrees out of phase the resulting voltage is E_1-E_1'. In general, the transmitters tend to synchronize but, because of the high reactance in the interconnecting ties, the operation is unstable, and the transmitters drift in and out of synchronism, thereby producing "beats" at a low frequency of between zero and five cycles per second. Under the condition of 180 degrees out-of-phase operation, station 2 transmitter has the effect of decreasing the voltage provided by station 1 transmitter from E_1 to E_1-E_1'. If the transmitter at station 1 is arranged to provide independent control over more than one feeder out of the station using series sequence coupling, then the value of E_1+E_1' should not exceed one volt.

The following analysis is usually employed to determine the magnitude of the interfering voltage such as produced by a transmitter at station 2. The interfering voltage is caused by a 720-cycle current flowing through the transformer bank and the line to station 3. At this point it may be considered that the current produces a voltage E_3 across an impedance combining the 720-cycle impedances of the generators and the outgoing lines terminated in the other substation loads. This voltage is then transmitted over the outgoing lines to reach the other substations. The first controller at each substation receives the maximum interfering voltage, but this is less than E_3 because of the voltage drop in transmission.

For estimating purposes E_1' is obtained from equation 8 which is based on the following assumptions, both of which tend to indicate a somewhat higher interfering voltage than would actually be obtained:

1. All the 720-cycle current from station 2 transmitter flows through the generators at station 3.

2. There is no 720-cycle voltage drop from station 3 to the first controller at station 1.

$$E_1' = E_3 = \frac{E_2\,(\text{station 2 60-cycle short-circuit kva})}{\text{station 3 60-cycle short-circuit kva}} \qquad (8)$$

Theoretically, intertransmitter interference becomes more pronounced on an interconnected system as the ratio of the total number of transmitters to the total number of substations increases, since the total potential interfering voltage becomes the sum of the interfering voltages produced by each transmitter operating alone. In practice, however, it is usually feasible to arrange the coupling circuits to avoid difficulty of this kind.

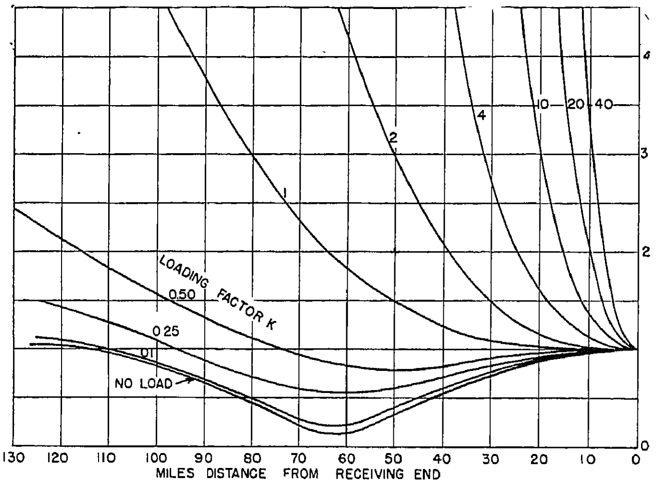


Figure 14. 720-cycle volts along uniformly loaded number-2 Brown and Sharpe overhead line with one volt at receiving end

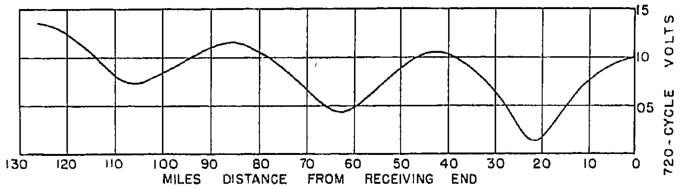


Figure 15. 720-cycle volts along number 2 Brown and Sharpe overhead line at no load with one volt at receiving end

Conductor capacitance increased to show effect of many short tap lines

As an example in the previous comparison of series and shunt coupling, it was shown that the voltage across the bus to which the feeder is connected is inherently lower using series coupling; therefore intertransmitter interfering voltages are lower. The relative interfering voltages produced vary greatly with the application conditions, but studies made of actual systems permit deducing some general conclusions:

1. Using series sequence coupling to control feeders individually at each substation, the intertransmitter interference in this type of application is almost negligible. By applying transmitters at each station so that each transmitter produces no objectionable interference over its own feeders, the slight additional interference caused by simultaneous operation of transmitters is usually acceptable.

2. Using series bus coupling in which each transmitter provides control over an entire bus or substation an intertransmitter interference problem may arise if two or more simultaneously operated transmitters provide control over 30 per cent or more of the total 60-cycle system load. However, this interference may be prevented effectively by the use of a 720-cycle shunt connected at some central point on the system (such as across station 3 bus in Figure 13) or by the use of 720-cycle shunts connected across the bus at some of the substations where transmitters are located. If this type of coupling is used at all substations on the system, to prevent intertransmitter interference one large 720-cycle shunt is required, or else smaller shunts are required at enough of the substations so that the total 60-cycle load capacity at these stations is at least 50 per cent of the total 60-cycle system peak load.

3. Using shunt coupling for control over individual substations, an intertransmitter interference problem may arise if two or more simultaneously operated transmitters provide control over ten per cent or more of the total 60-cycle system load. This means practically that interference preventive measures are almost always required if two or more shunt coupled transmitters are applied on the same system. As an example, if the maximum-current tuning arrangement of shunt coupling is selected to permit use of a smaller converter, the coupling circuit tends to act as a 720-cycle shunt drawing a leading current which increases the interfering voltage. In contrast, if constant voltage tuning is used with a resulting larger converter, the use of auxiliary circuits allows the coupling circuit to act as a 720-cycle shunt tuned for minimum 720-cycle impedance which tends to reduce the incoming interfering signal. However, with a strong interfering signal a larger coupling equipment may be required to allow for the additional 720-cycle current that flows through the coupling circuit, and this form of operation makes inadequate provision for preventing interference to other transmitters using series coupling on the same 60-cycle system.

For these reasons, traps are often required in series with the substation transformer bank when shunt coupling is used, in order to reduce the intertransmitter interference to the value often inherently provided by series coupling alone and, as always, provided by series coupling plus a 720-cycle bus shunt.

Shunt coupling is ideal for applications which call for one transmitter to provide control over an entire system which has no interconnections to other systems.

Effect of Capacitance and Long Lines

720-Cycle Voltage Distribution Along Long Lines at No Load

On long lines the effect of capacitance and reflection must be considered. Figure 14 shows the variations in 720-cycle voltage along an overhead line with one volt at the receiving end for various loading factors. The scale of distance is taken from the receiving end because the 720-cycle voltage at any point is the resultant voltage of a direct voltage that would exist there if the line were infinitely long, plus a reflected voltage whose magnitude and phase are dependent on the distance from the point to the actual receiving end of the line.

For the curve marked "no load," the only line load present is that due to line capacitance and leakage. If the 720-cycle transmitter is located 31 miles (one-eighth wave length) distant from the receiving or far end of the line, this curve shows that a transmitted voltage of 0.7 volt is required for one volt at the receiving end. At intermediate points the the voltage can be read directly off this curve based on the distance from each point to the receiving end, the general characteristic being a steady rise in voltage from the transmitter all the way to the far end of the line.

Similarly, for a line length of 62 miles and a transmitted voltage of 0.14 volt, the line voltage rises steadily to one volt at the receiving end. With the line length between 62 and 124 miles a steady voltage drop occurs until an intermediate point is reached beyond which a steady voltage rise occurs up to the receiving end. Thus for a line 124 miles long and a transmitted voltage of 1.05 volts, the voltage falls to 0.14 volt at the center of the line and then rises to one volt at the far end.

The wave length of the line is not appreciably affected, and the general shape of the no-load curve is the same for a considerable range of overhead conductor spacings and sizes. Assuming the line leakage is constant, the minimum voltage point of 0.14 volt for the number 2 Brown and Sharpe conductor becomes, respectively, 0.5 volt for number 10, or 0.08 volt for 4/0 conductor. If 1.05 volts is transmitted over a 125-mile 4/0 line, the voltage at the center of the line is reduced to 0.08 volt because of reflection or so-called standing-wave effects. Hence, to provide the required minimum of 1.75 volts 720 cycles at all points on the line, the transmitted voltage required on the same 115-volt 60-cycle base is 23 volts, without any allowance for distribution transformer or secondary voltage drop. This voltage corresponds to 20 per cent of the 60-cycle line voltage and would be impractical to apply for a number of reasons. A somewhat more practical solution would involve terminating the line in a 720-cycle shunt having an impedance equal to the characteristic line impedance (approximately 300 ohms resistance based on one conductor to neutral). The result would then be a small but continuous voltage drop to the receiving end of the line. However, this solution rarely is required on long distribution feeders for reasons outlined in the following paragraphs.

720-Cycle Voltage Distribution Along Long Distribution Feeders

On distribution feeders there is almost always some 60-cycle load present, and even at "no load" the 720-cycle controllers alone impose some load as previously explained. However, it is feasible to use the different curves in Figure 14, when the load approximates a uniform distribution so that loading factor K can be computed from equation 9.

$$K=\frac{\text{kva}}{(\text{kv})^2} \qquad (9)$$

where

kva = three-phase 60-cycle load kilovolt-amperes per mile (for single-phase feeders the per-mile per-conductor load impedance is reduced 50 per cent, and twice the actual load kilovolt-amperes per mile is then used in equation 9)

kv = 60-cycle line-to-line kilovolts

For convenience in visualizing the significance of the curves in Figure 14, assume that the feeder is a one-kilovolt three-phase 60-cycle feeder. Values of loading factor then are equal to the actual 60-cycle load kilovolt-amperes per mile. When a three-phase 60-cycle load of 0.1 kilovolt-ampere per mile is added to a 125 mile unloaded number 2 Brown and Sharpe three-phase feeder the voltage at the center of the feeder increases from 0.14 to 0.23 volt, while requiring only a slight increase in transmitted volts. For a 60-cycle load of one kilovolt-ampere per mile the standing-wave effects are almost negligible, regardless of the feeder length. The maximum error in neglecting completely the reflected voltage at a point 40 miles from the receiving end of the line where the greatest error is obtained is only 15 per cent.

Feeders on which standing-wave effects are prominent only at light load present a simple application problem, provided transmission is only required at heavy or medium load, such as for off-peak load control. Street or highway lights are seldom controlled at periods of minimum load because of the power-demand characteristics of rural customers.

Another factor influencing the 720-cycle voltage distribution along long feeders is the presence of tap lines. Frequently long lightly loaded lines with many taps are encountered in rural distribution. The taps are generally less than one-eighth wave length (31 miles) long so it is unlikely that reflection from the end of an unloaded tap may cause a minimum voltage point along the tap, or that an unloaded tap line one-quarter wave length long may have a short-circuiting effect on the main line.

Very short tap lines can be represented electrically by a lumped capacitance connected across the main feeder. Figure 15 is based on a line having eight miles of very short tap lines for each mile of line. Since the line capacitance has been increased nine times by adding the tap lines, we might expect that the wave length, which is dependent inversely on LC, would be decreased to one third its former value, and a comparison of Figure 15 with the no-load curve in Figure 14 indicates that this is exactly what happens. The low-voltage point of 0.14 volt at a distance of 21 miles is exactly the same as for the corresponding point at 63 miles on the no-load curve of Figure 14.

In practice tap lines are rarely ever so short that they do not improve the voltage distribution along a long line. The voltage along the line is the resultant of a direct voltage received directly from the transmitter and the voltages reflected from the ends of the main line and each tap line. The reflected voltages are often received out of phase because of the different lengths of the paths traversed. Therefore, if the line is long enough to have one or more points of minimum voltage, the minimum voltage is higher than for a line of the same wave length with no taps. In other words, tap lines generally improve the voltage distribution along lightly loaded long lines, and if taps are present in sufficient numbers, 720-cycle standing waves are completely absent.

Cable Distribution Feeders

Because of the reduced conductor reactance and increased capacitance of cable as compared with overhead lines, short cable feeders generally show considerably less 720-cycle voltage drop for the same loading, but the 720-cycle performance generally can be estimated using the methods suggested previously for short overhead lines. However, this disregard of the distributed capacitance generally tends to give pessimistic results regarding 720-cycle voltage drop and transmitted power, and the conclusions may require a larger 720-cycle transmitting equipment than is required actually.

Cables are seldom encountered on rural or other long, lightly loaded lines, but at 720 cycles cable is superior to overhead lines, because its higher attenuation gives it more freedom from standing-wave effects and results in a more uniform voltage distribution.

Effect of Shunt Capacitors at 720 Cycles

The reactance of a capacitor is inversely proportional to frequency. If the frequency is increased from 60 to 720 cycles, the capacitor current increases 12 times for the same voltage. The general effects of capacitors are outlined in the following paragraphs. Where these effects are adverse to the 720-cycle operation, they can always be eliminated by taking corrective measurements at the capacitors. (Refer to "720-Cycle Corrective Measures for Shunt Feeder Capacitors.")

Bus Capacitors

If a bus capacitor is present at a substation it usually can be used to assist in the application of a carrier transmitter at the substation. If series coupling is used, it then is generally preferable to install the coupling transformers in the feeder so that the capacitor is on the bus side of the coupling transformers. If Figure 8 is used to determine the required feeder 720-cycle volts, it can be considered that each capacitor 60-cycle kilovolt-ampere at the aforementioned location has the effect at 720 cycles of reducing the bus

60-cycle short-circuit kilovolt-ampere by 144 kva. If the capacitor kilovolt-amperes is sufficient to reduce the apparent short-circuit kilovolt-amperes to zero, this means that the capacitor and the 60-cycle generator (or station transformer) act as a parallel resonant trap circuit of high impedance at 720 cycles. As previously explained, a high bus impedance is undesirable when series coupling is used; but, by using a reactor (having 0.7 of one per cent 60-cycle reactance on the capacitor base) in series with the capacitor, a low impedance 720-cycle bus shunt is obtained which is very beneficial to the 720-cycle operation with series coupling.

If other factors favor the use of shunt coupling at a substation having a bus capacitor, this capacitor usually can be used as a part of of the coupling capacitor assembly.

Primary-Feeder Capacitors

Primary-feeder capacitors generally have the effect of increasing the length of the line. Thus a short line with a large capacitor may have some of the standing-wave effects obtained on long lines without capacitors. It is well known that at 60 cycles the flow of leading current through the line reactance causes the capacitors to give a voltage rise which is fixed and largely independent of the load. If the line reactance is considerably less than the value required for series 720-cycle resonance, the preceding is true also at 720 cycles. Thus, if a 60-cycle voltage rise of 0.2 of one per cent is obtained, the line reactance is increased 12 times, the capacitor reactance is reduced to one twelfth of the 60-cycle value and the per cent voltage rise is increased 144 times at 720 cycles. Hence the 720-cycle voltage rise is approximately 29 per cent under the same conditions.

Under 60-cycle conditions of no feeder load and a line reactance of 0.7 of one per cent on the capacitor base, the line and capacitor reactances are equal and form a series resonant circuit at 720 cycles in which the current flow is limited only by the line-conductor resistance. Hence the capacitor greatly reduces the feeder 720-cycle impedance and causes a rapid rise in voltage along the feeder ending in a high voltage at the capacitor.

Under 60-cycle conditions of no feeder load and a line reactance greater than 0.7 of one per cent on the capacitor base, the line reactance is greater than the capacitor reactance at 720 cycles and the feeder 720-cycle input current becomes lagging. Starting at the substation and moving toward the capacitor, the feeder 720-cycle voltage then gradually decreases until a low-voltage point is reached at some intermediate point and then gradually rises to some higher value at the capacitor. The lowest voltage is obtained at a point where the line reactance from the point to the capacitor form a series resonant circuit at 720 cycles with the capacitor reactance.

Under full-load conditions the load usually prevents excessive voltage rise or pronounced resonance effects, but under light-load conditions these effects must be considered. Under some conditions, the presence of feeder capacitors may require more 720-cycle power input under light-load conditions than under heavy loads.

Where it is feasible to control the size and location of capacitors, successful 720-cycle operation often can be obtained in spite of large primary feeder capacitors without taking corrective measures at the capacitors. Many feeders take the form of a line running to a load center with the load distributed in a circle about the load center. On a feeder of this type the operation of 720-cycle controllers is not required between the station and the load center, so the 720-cycle voltage distribution is unimportant along this part of the circuit. If the capacitor is located between the station and the load center so that a 60-cycle voltage rise of approximately 1.4 per cent is obtained at the capacitor, the 720-cycle capacitor reactance is one-half the line reactance from the station to the capacitor, and the 720-cycle volts received at the capacitor is equal to the feeder 720-cycle input volts. The 720-cycle voltage distribution beyond the capacitor would then be the same as on a similar feeder without a capacitor with no drop in voltage to the load center. For a 4,000-volt three-phase feeder these conditions would exist for a three-phase capacitor of 400 kva at a distance from the station of approximately one mile, or 200 kva at a distance of approximately two miles.

Note that Figures 5, 8, and 10 do not apply if feeder capacitors present are not equipped with suitable reactors, unless the conditions outlined heretofore make negligible their effect on the 720-cycle characteristics of the feeder. If the 720-cycle application is made without reactors for the capacitors present, the 720-cycle feeder voltage, and power required, and the choice between series and shunt coupling should be based on a careful analysis of the 720-cycle impedance diagram of the 60-cycle system.

Secondary-Feeder Capacitors

Some distinction can be made between secondary-feeder capacitors depending on whether control is or is not required on the secondary feeder on which the capacitor is located. If control is not required, it is only necessary to consider the conditions under which the distribution-transformer reactance, capacitor reactance, and secondary load may combine to affect adversely the 720-cycle voltage at other points on the primary feeder. If the capacitor is large enough to short-circuit essentially the transformer at 720 cycles, the transformer reactance usually prevents appreciable adverse effect on the voltage on the primary feeder. Troubles with this setup are relatively infrequent, since the circuit constants usually must be near series resonance before the 720-cycle voltage is affected. If control is required over the secondary feeder on which the capacitor is located, the capacitor may require 720-cycle correction, if it causes either series resonance or low voltage because of short-circuiting effect.

720-Cycle Corrective Measures for Shunt-Feeder Capacitors

The preferred methods of eliminating adverse 720-cycle effects of shunt capacitors involve the use of reactors in series with the capacitors. One method is to use a reactor of approximately five per cent 60-cycle reactance on the capacitor base in every capacitor. The reactor increases the capacitor voltage five per cent, the capacitor kilovolt-amperes is increased ten per cent, and the net correction kilovolt-amperes is increased five per cent at 60 cycles, which may require that the capacitor be designed for a slightly higher 60-cycle voltage. The capacitor-reactor combination acts as a reactor at 720 cycles. The size of the reactor required and the fact that the 720-cycle transmitter has to furnish reactive vars absorbed in the reactor make this method relatively undesirable.

A series reactor having a 60-cycle reactance of 0.7 of one per cent on the capacitor base has a reactance equal and opposite to the capacitor at 720 cycles. If a reactor of twice this reactance (1.4 per cent at 60 cycles) is connected in series with the capacitor, the combination acts as a reactor whose reactance is equal to the reactance of the capacitor but opposite in sign. Hence the combination will neutralize or correct the 720-cycle power factor of another equal capacitor at the same location. Thus, if 1.4 per cent 60-cycle reactors are installed in one-half the capacitors, the sum of the 720-cycle current flowing through all capacitors is zero. Preferably, reactors should be furnished for one half of the capacitors at each capacitor bank. Where this is not feasible, capacitors with and without reactors need not be at the same location but the distance between locations must receive some consideration because of the circulating 720-cycle current which must flow in this path.

In some cases the preceding arrangement can be used to improve the 720-cycle feeder voltage distribution. By using tapped reactors the 720-cycle current through the reactor can be adjusted to obtain a resulting leading current through all capacitors and thus obtain a desirable 720-cycle voltage rise which will compensate for the voltage drop because of the feeder load and thus permit the use of smaller transmitter coupling and converter equipment.

In short, any capacitor arrangement that improves 60-cycle operation can be used to also improve 720-cycle operation.

Appendix

In order to illustrate the use of the equations and curves in typical 720-cycle control applications, the following examples are given.

Example 1

The 4,000-volt feeder circuit shown in Figure 16 is given. The three-phase 720-cycle voltage and power required at the substation end of the feeder to provide 720-cycle control at all points on the 115- or 230-volt secondary circuits is determined as follows:

The 60-cycle $\%IX$ may be computed for peak-load conditions for each section of line using equation 4. For example, use section 3 in Figure 16.

kva = 500

$d = 1$ mile

$X = 0.762$ ohm per mile

kv = 4.0

$$\text{60-cycle } \%IX = \frac{500\times1\times0.762}{10\times4^2} = 2.4 \text{ per cent}$$

Similar values for each section of the line are shown in Table I.

Note that voltage regulators have the small value of approximately one-half $\%IX$ in maximum boost position, but they provide enough boost in 720-cycle voltage so that they can be ignored conservatively.

The last transformer on this feeder is obviously at the far end of section 4. It is assumed that the last customer is fed from a distribution transformer having 2.6 $\%IX$ at the peak load through a secondary feeder having three $\%IX$. The latter value is often difficult to determine accurately, but a reasonable error in its determination usually will have little effect on the over-all application.

Table I

Section	Kva Load	X Ohms Per Mile	60-Cycle %IX
Regulator	1,000		0
1	1,000	0.712	5.6
2	200	0.762	1.0
3	500	0.762	2.4
4	200	0.762	1.0

The total 60-cycle $\%IX$ from the substation bus to the last customer is the sum of the $\%IX$ values for each part of the circuit traversed so that:

Total 60-cycle $\%IX = 5.6+2.4+1+2.6+3 = 14.6$ per cent

From Figure 3 it is seen that, for a value of 14.6 $\%IX$, the minimum 720-cycle feeder input volts necessary to provide complete 720-cycle control is:

Minimum $E_f = 3.5$ volts on 115-volt 60-cycle base

$$= 3.5\times\frac{4,000}{115} = 121 \text{ wye-volts}$$

on 4,000 wye-volt 60-cycle base

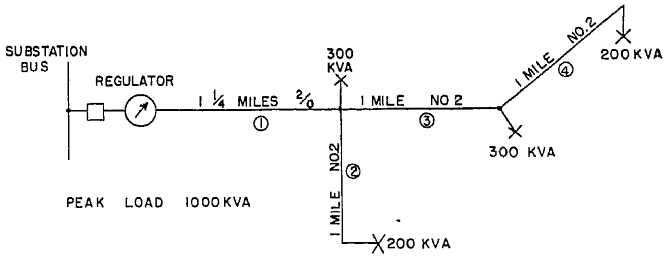


Figure 16. Circuit diagram of 4,000-volt three-phase 60-cycle overhead feeder showing approximate peak-load distribution

It is convenient to obtain the feeder 60-cycle reactance for an equivalent concentrated load circuit using values in Table I.

Equivalent primary-feeder reactance

$$= \frac{1,000\times5.6+200\times1+500\times2.4+200\times1}{1,000}$$

= 7.2 per cent on 1,000-kva base

Adding the average transformer reactance (estimated at two per cent) and average secondary-feeder reactance (estimated at one per cent) to this value, 10.2 per cent equivalent reactance to the load is obtained. Entering Figure 5 with this reactance and correcting for 3.5 volts required, rather than five volts, the minimum required feeder input is

720-cycle three-phase kilovolt-amperes

$$= 1.18\left(\frac{3.5}{5}\right)^2 = 0.58$$

720-cycle three-phase kilowatts

$$= 0.75\left(\frac{3.5}{5}\right)^2 = 0.37$$

Example 2

Assume that 720-cycle control is desired at all points on the 115- or 230-volt secondary circuits of a 1,200-kva radial feeder which has not been constructed and that no prints are available of the feeder layout or load distribution. Only the following data are known:

(*a*). The average 60-cycle load power factor is 70 per cent lagging.

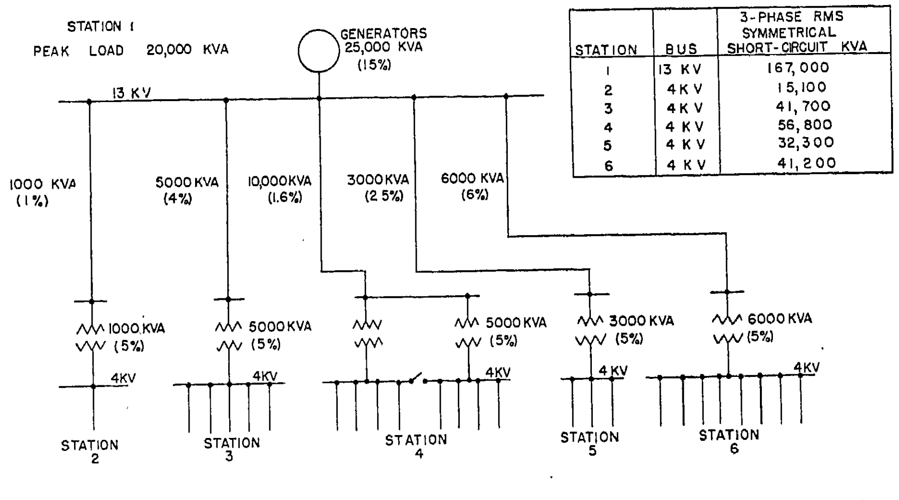
(*b*). 00 primary and 0 secondary conductors are to be used.

(*c*). To hold the customer's 60-cycle voltage within reasonable limits the peak-load 60-cycle voltage drop to the first customer is approximately ten per cent and cannot exceed 15 per cent to the last customer.

From wire-table values of X/R of 1.61 (for 00 conductor) and 0.942 (for 0 conductor) are obtained. Since most of the 60-cycle voltage drop occurs in the primary 00 conductor, it is estimated that an average value of X/R of 1.5 is applicable for the total voltage drop. For a value of X/R of 1.5 at 70 per cent power factor, 0.85 $\%IX$ is

Figure 17. Circuit diagram of three-phase 60-cycle power system

60-cycle per cent reactances shown in parentheses



STATION	BUS	3-PHASE RMS SYMMETRICAL SHORT-CIRCUIT KVA
1	13 KV	167,000
2	4 KV	15,100
3	4 KV	41,700
4	4 KV	56,800
5	4 KV	32,300
6	4 KV	41,200

obtained from Figure 4. Hence the total $\% IX$ to the last customer is

Total 60-cycle $\% IX = 0.85 \times 15 = 12.7$ per cent

The minimum required $E_f = 3.2$ volts (from Figure 3)

Equivalent reactance (equation 5)

$$= \% IX\,({}^2/_3 V_{d1} + {}^1/_3 V_{d2})$$
$$= 0.85\,({}^2/_3 \times 10 + {}^1/_3 \times 15)$$
$$= 9.9\% \text{ on 1,200-kva 60-cycle base}$$

Entering Figure 5 with this reactance and correcting for 3.2 volts instead of five volts and for 1,200 instead of 1,000-kva load, the minimum required feeder input is

720-cycle kilovolt-amperes

$$= 1.21\left(\frac{3.2}{5}\right)^2 \times 1.2 = 0.60$$

720-cycle kilowatts

$$= 0.78\left(\frac{3.2}{5}\right)^2 \times 1.2 = 0.38$$

Example 3

The system and system characteristics shown in Figure 17 are given. Series coupling is to be applied to provide control over the four-kilovolt feeder out of station 2.

Since the station-transformer-bank rating is 1,000 kva, it is assumed that control is required up to 1,000-kva station load. The 720-cycle operating characteristics of this system are obtained easily from the following:

The 4-kv bus short-circuit kva = 15,100 kva (from Figure 17)

$$P = \frac{\text{short-circuit kva}}{\text{load kva on feeder being controlled}} = \frac{15{,}100}{1{,}000} = 15.1$$

$$N = \frac{\text{load kva on other feeders on same bus}}{\text{load kva on feeder being controlled}} = \frac{0}{1{,}000} = 0$$

$E_f = 3.48$ volts (from Figure 8)

Since this voltage is less than the four volts usually considered necessary, the required voltage should be determined as in examples 1 or 2.

Example 4

The system and system characteristics shown in Figure 17 are given. It is required to determine the interfering voltages between transmitters at stations 2 and 3 to provide simultaneous control over all feeders at each station using series bus coupling.

$E_f = 3.48$ volts, 720 cycles (from example 3)

$E_b = 5 - 3.48 = 1.52$ volts on 4-kv bus

From Figure 17 the 60-cycle short-circuit kilovolt-amperes on station 2 bus and at the 13-kv generator are, respectively, 15,100 and 167,000 kva. From equation 8 the interfering voltage does not exceed

Volts at station 3 from station 2 transmitter

$$= \frac{1.52 \times 15{,}100}{167{,}000} = 0.14 \text{ volt}$$

Repeating the previous calculations for station 3 transmitter, it can be shown that

Volts at station 2 from station 3 transmitter $= 0.55$ volt

If the two transmitters are operated at different times the aforementioned interfering voltages are both less than one volt, and hence no objectionable interference is obtained.

If the two transmitters are operated simultaneously and happen to synchronize 180 degrees out of phase, it can be concluded that:

(*a*). The voltage on the four-kilovolt feeders at station 3 is decreased 0.14 volt by station 2 transmitter.

(*b*). The voltage on the four-kilovolt feeders at station 2 is decreased 0.55 volt by station 3 transmitter.

(*c*). Each transmitter must provide a somewhat higher 720-cycle voltage than would otherwise be required without interference.

Example 5

The system and system characteristics shown in Figure 17 are given. Series sequence coupling is to be applied to provide independent control over two or more four-kilovolt feeders out of station 3.

Table II

Station	Feeder Load Kilovolt-Amperes	N	4-Kv Short-Circuit Kilovolt-Amperes	720-Cycle Volts on 115-Volt 60-Cycle Base: 4-Kv Feeder	4-Kv Bus	13-Kv Generator
2	1,000	0	15,400	3.51	1.49	0.115
3	1,000	4	43,500	4.60	0.40	0.087
4	1,250	3	60,200	4.51	0.49	0.13
6	1,000	5	42,800	4.62	0.38	0.082

It is first necessary to determine the limiting conditions for use in Figure 8. The most inefficient coupling will be obtained when the 720-cycle voltage is impressed on the most heavily loaded feeder at the peak load and when the least load is present on the other feeders. When the ultimate capacity of the station of 5,000 kva is reached, assuming control is required over any feeder up to its full-rated 1,380-kva load, the total load on the four other feeders is 3,620 kva. On this basis the limiting conditions for use in Figure 8 are:

$$P = \frac{\text{4-kv bus short-circuit kva}}{\text{load kva on feeder being controlled}} = \frac{41{,}700}{1{,}380} = 30.2$$

$$N = \frac{\text{load kva on other feeders}}{\text{load kva on feeder being controlled}} = \frac{3{,}620}{1{,}380} = 2.6$$

$E_f = 4.33$ volts on 4-kv feeder (from Figure 8)

$E_b = 5 - 4.33 = 0.67$ volt on 4-kv bus

This voltage is considerably less than one volt, hence independent control over each feeder under the assumed limiting conditions will be obtained. Also, since E_f is greater than four volts, ample 720-cycle feeder voltage is available assuming the feeders are of the usual type.

Example 6

The system and system characteristics shown in Figure 17 are given. It is required to determine the interfering voltages produced by transmitters at two or more stations using series sequence feeder coupling at each station.

Following the method used in example 5 it can be shown easily that station 5 transmitter is near the point of having interference between its own feeders. A 720-cycle shunt is required across the station 5 four-kilovolt bus, if additional transmitters are to be operated simultaneously. If this shunt is used, station 5 can be ignored as a transmitter or receiver of interfering voltage. Also this shunt passes some 720-cycle current from transmitters at other stations so as to decrease the current through the 13-kv generators and reduce the interfering voltage. The 720-cycle effect is the same as though the 167,000 short-circuit kilovolt-amperes of the 13-kv generators is increased directly by the 32,300 short-circuit kilovolt-amperes rating of station 5. Hence, for this study, the 13-kv generators can be replaced by a new generator having a 60-cycle short-circuit kilovolt-amperes rating of 199,300 kva and a reactance of 12.5 per cent on a 25,000-kva base.

The four-kilovolt bus short-circuit kilovolt-amperes at station 4 based on the new generator then is

$$\frac{25{,}000 \times 100}{12.5 + \frac{25{,}000}{5{,}000}\left(5 + \frac{1.6 \times 5{,}000}{10{,}000}\right)} = 60{,}200 \text{ kva}$$

If station 4 transmitter is operated under maximum-station-load conditions with equal loads on each feeder the limiting conditions are

$$P = \frac{\text{4-kv bus short-circuit kva}}{\text{load kva on feeder being controlled}} = \frac{60{,}200}{1{,}250} = 48$$

$$N = \frac{\text{load kva on other feeders}}{\text{load kva on feeder being controlled}} = \frac{3{,}750}{1{,}250} = 3$$

$E_f = 4.51$ volts on 4-kv feeder (from Figure 8)

$E_b = 5 - 4.51 = 0.49$ volt on 4-kv bus

$E_g = 0.49 \times \frac{53{,}800}{199{,}300} = 0.13$ volt on 13-kv generator

Values of E_f and E_b operating any one of the transmitters based on maximum load at each station and feeders equally loaded at each station are tabulated in Table II. Station 5 is omitted because a 720-cycle bus shunt is to be used at this station.

Disregarding station 2 because it has only one feeder, it is apparent that the maximum interfering voltage on any feeder at any station occurs at station 4 which has a four-kilovolt bus voltage of 0.49 volt when its transmitter is operated alone. If the transmitters at stations 2, 3, and 6 are simultaneously operated and synchronized in phase, the resultant voltage does not exceed

$$\text{720-cycle volts on 13-kv generator} = 0.115+0.087+0.082=0.28 \text{ volt}$$

If station 4 transmitter is operated at the same time, the interfering voltage at station 4 under this condition does not exceed

$$0.49+0.28=0.77 \text{ volt}$$

Since the total interfering voltage is less than one volt, no objectionable interference is obtained, even if all transmitters are operated simultaneously. In an actual application available station and feeder load data should be consulted and applied in the preceding calculations to ensure that the interfering 720-cycle voltage has been obtained under the most unfavorable operating conditions.

Example 7

The system and system characteristics shown in Figure 17 are given. A shunt-coupled transmitter is to be applied at station 1 to provide control at stations 2 to 6, inclusive.

In this example the 720-cycle voltage is impressed across station 1 13-kv bus. This arrangement provides operating flexibility in that the four-kilovolt and 13-kv feeders out of the different substations can be interconnected in a primary or low-voltage secondary network without increasing the 720-cycle voltage or power required. If each substation carries rated load, the lowest 720-cycle voltage is obtained at station 6, since the highest 60-cycle $\%IX$ is obtained at this station.

$$\text{60-cycle } \%IX \text{ to station } 6=6+5 = 11 \text{ per cent}$$

If it is assumed that the feeder in example 1 is the feeder at station 6 requiring the highest feeder 720-cycle voltage, the maximum 60-cycle $\%IX$ from station 6 four-kilovolt bus to the last controller is 14.6 per cent. Adding this to the foregoing $\%IX$ to station 6 gives 25.6 $\%IX$ from station 6 to the last controller.

$$\text{720-cycle volts at station } 1=5.7 \text{ volts (from Figure 3)}$$

The maximum 720-cycle power will be obtained with 25,000 kva of generators in service at the system peak load of 20,000 kva. At this time the average load at each substation is approximately 75 per cent of the station transformer kilovolt-ampere rating. The 60-cycle per cent reactance to reach the substation four-kilovolt busses varies for each substation, but the maximum and minimum values are

Per cent reactance for station 6
$=0.75(6+5)$
$=8$ per cent on 4,500-kva base

Per cent reactance for station 2
$=0.75(1+5)$
$=4.5$ per cent on 750-kva base

For estimating purposes it can be assumed that the per cent reactance to the average substation falls midway between the aforementioned two values so that a value of six per cent is used. Representing each four-kilovolt feeder by a concentrated load with ten per cent series reactance, the total 720-cycle power input in the direction of the loads can be determined, based on 16 per cent reactance on a 20,000-kva base. The curves of Figure 10 are based on ten rather than 16 per cent reactance but may be used for estimating purposes since this assures results on the safe side. (If more accurate results are required, suitable corrections can be determined from Figure 5.) From Figure 17, 20,000 kva (the 13-kv 60-cycle peak load) and 167,000 kva (the station 1 13-kv 60-cycle short-circuit kilovolt-amperes) are the two values used in Figure 10 to obtain 48-kva 720-cycle input at 0.47 power factor, for five volts impressed.

But the peak load can be carried with one 5,000-kva generator out of service. This would decrease the 60-cycle short-circuit kilovolt-amperes 20 per cent to 134,000 kva and the 720-cycle kilovolt-amperes approximately ten per cent to 44 kva (from Figure 10). Thus a voltage multiplier of 1.05 from Figure 12 must be applied. The required three-phase 720-cycle input to the 13-kv bus at station 1 is:

Volts on 115-volt 60-cycle base
$=5.7\times1.05=6$ volts

Volts on 13-kv 60-cycle base

$$=\frac{6\times13{,}000}{115}=678 \text{ volts}$$

$$\text{Kilovolt-amperes}=48\left(\frac{6}{5}\right)^2=69 \text{ kva}$$

$$\text{Kilowatts}=69\times0.47=32.4 \text{ kw}$$

$$\text{Current}=\frac{69\times1{,}000}{3\times678}=59 \text{ amperes}$$

References

1. STANDARD HANDBOOK FOR ELECTRICAL ENGINEERS (book). McGraw-Hill Book Company, Inc., New York, N. Y.

2. IT CAN BE DONE, **J. B. Martin.** *Electric Light and Power,* November 1936, pages 27–9.

3. WHAT TYPE OF CONTROL IS BEST FOR THE OFF-PEAK WATER-HEATER LOAD, **F. M. Starr.** *Municipal Utilities,* September 1935, pages 6–8.

4. CARRIER-CURRENT CONTROLS HOLLYWOOD STREET LIGHTS, **O. W. Holden.** *Electrical World,* December 1935, pages 2886–8.

5. SPRINGFIELD'S CARRIER-CURRENT STREET-LIGHT CONTROL SUCCEEDS, **R. E. Curtis.** *Electrical World,* December 26, 1931, pages 1130–2.

6. THE CENTRALIZED CONTROL OF PUBLIC LIGHTING AND OFF-PEAK LOADS BY SUPERIMPOSED RIPPLES, **H. Purslove Barker.** *Journal* of the Institution of Electrical Engineers (London, England), December 1938, pages 823–36.

7. RIPPLAY, **R. M. A. Smith,** *Metropolitan-Vickers Gazette,* October 1940, pages 76–80.

8. THE TELENERG-SYSTEM, **W. zur Megede.** *Siemens Zeitschrift* (Berlin, Germany), July–August 1933, pages 165–9.

A New Control System for Automatic Parallel Operation of Load-Ratio-Control Transformers

Discussion and authors' closure of paper 43-64 by S. Minneci and S. B. Farnham, presented at the AIEE North Eastern District technical meeting, Pittsfield, Mass., April 8–9, 1943, and published in AIEE TRANSACTIONS, 1943, July section, volume 62, pages 488–93.

C. E. Winegartner (Cleveland Electric Illuminating Company, Cleveland, Ohio): This ingenous control system is designed specially to secure the seven characteristics, as stated in the summary of the paper, which are particularly applicable to medium-voltage networks. It might be appropriate to consider some of its limitations and to compare it with electrically interlocked units with duplicate master-control equipment.

It is not practical to set contact-making voltmeters closer than about $\pm 1\frac{1}{4}$ volts. Although the circulating current is self-reducing, there will still be, at times, this band of voltage producing it. Also, the meter has the dual function of regulating voltage and the relative tap position of the transformers. This does not provide as positive an interlock as does an odd and even auxiliary switch on the tap-changer mechanism operating through a relay to prevent further automatic operation when units get out of step. In fact, a jammed or welded contact on the meter will cause a transformer to run to its tap limit instead of being blocked as soon as it gets one step out.

Even though the odd- and even-step electrical interlock does not provide for reducing circulating current automatically, it is possible to operate the transformers any multiple of two steps, continuously out of step, to afford some control, as long as the transformers do not run to their limits. In view of the aforementioned limitations on the automatic control, a fixed relation between steps through the electrical interlock will, in some cases, keep circulating currents down as effectively as automatic control.

Duplicate master control has its greatest advantage when two or more transformers are operated in parallel and are located relatively close together. This always affords a spare automatic-control equipment to use when one has to be taken out of service for tests or maintenance. If the current from all the transformers in parallel is totalized, and if this current is used through the duplicate compensators, it is not necessary to change compensator settings when a transformer is taken out of service, and compensation is not affected by circulating current.

To summarize, duplicate master control, at a saving of compensator and auxiliary current transformers for each of two load-ratio-control transformers, and without requiring additional control equipment for additional units, affords spare control equipment for any emergency. While it does not afford automatic control of circulating current, yet where it is practical to have a central control point for all transformers in parallel and where circulating currents do not become excessive, it does have definite merit.

S. Minneci and S. B. Farnham: Mr. Winegartner's discussion is based on his familiarity with an arrangement for parallel operation of regulating equipment in which the units are electrically interlocked so as to operate substantially as one unit; and at any given time all are under the control of one master-control equipment, with another control equipment available as a spare. Although this type of arrangement has been used successfully in a number of applications, it requires the attention of an operator each time a unit is brought into parallel operation with the others, after having been out of service for any reason. The reason for this is that all of the regulating mechanisms must be brought by manual control to the same relative position before the master control is placed in operation. There is no way in which they can be brought into agreement automatically afterwards, and if they fall out of step the effective regulating range of the entire group is reduced by an amount equal to the greatest difference in position between any two units. In addition, there may be, of course, excessive circulating current, and the flexibility of operation is not so great as it is with some other arrangements. In several recent applications for which the electrically interlocked arrangement was being considered, these were among its principal limitations which lead to the development of the new fully automatic-control system described in the paper.

The authors agree with Mr. Winegartner, however, that the arrangement which he describes has certain merits, and may be operated successfully, provided:

(*a*). The transformers are connected directly in parallel on both their high-voltage and low-voltage sides, so as to avoid any differences in the applied voltages such as might be caused by different impedances in the supply circuits.

(*b*). Individual operation is not required.

(*c*). The number of units operated in parallel is not so great as to complicate unduly the control interconnections.

(*d*). There is no objection to having an idle transformer brought by manual control to the same operating position as the active units.

Mr. Winegartner makes the statement that it is not practical to set contact-making voltmeters closer than about $\pm 1\frac{1}{4}$ volts, which corresponds to a band width of $2\frac{1}{2}$ volts. This is probably a reasonable figure, if the regulator steps are $1\frac{1}{4}$ per cent. This corresponds to the rule of thumb to the effect that the voltmeter band width should equal the voltage difference between successive steps, plus one volt. On a 120-volt base, $1\frac{1}{4}$ per cent $= 1\frac{1}{2}$ volts, which, added to 1 volt, gives $2\frac{1}{2}$ volts as the band width, when Mr. Winegartner's figure is checked. However, there are many regulators having steps smaller than $1\frac{1}{4}$ per cent, and, consequently, narrower band widths are very frequently encountered. This is particularly true in the case of induction-type regulators, where band-width adjustment need not be influenced by consideration of frequency of operation in order to conserve the life of moving contacts.

It appears that Mr. Winegartner has overlooked the provision that is made in the new control system for using an overcurrent lockout relay, connected so as to respond to circulating current only, for insuring that no regulating mechanism can get more than a predetermined amount out of agreement with the others. His statement, that a jammed or welded contact on the contact-making voltmeter will cause a mechanism to run to its limit, therefore, does not apply to this new control system.

In the comparative evaluation of the two control arrangements, it will be found that the new system not only fulfills all of the functions of the electrically interlocked arrangement described by Mr. Winegartner, but also includes other features for which the electrically interlocked arrangement makes no provision. In addition, it is fully automatic, rather than requiring the services of an operator. These advantages, together with the high degree of flexibility for meeting varied system-operating conditions, result from the inclusion of a complete set of automatic-control equipment in each unit, in preference to placing dependence upon equipment that is located elsewhere. The additional auxiliary current transformer and compensator, required by the new control system, are operated in the secondary circuit of the main current transformer and are very small physically. Their added cost is usually insignificant.

It may be concluded that the new control system will supersede the electrically interlocked arrangement in the majority of applications.

Use of Equivalent Annual Ambient Temperature in Overloading Transformers and Voltage Regulators

Discussion and author's closure of paper 43-65 by M. S. Oldacre, presented at the AIEE North Eastern District technical meeting, Pittsfield, Mass., April 8–9, 1943, and published in AIEE TRANSACTIONS, 1943, July section, pages 477–80.

W. C. Sealey (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The described method for determining the equivalent ambient temperatures has a sound theoretical as well as empirical basis. The values of equivalent annual ambient temperatures listed in the paper are average equivalent ambient temperatures based on an average year.

The author is strictly correct in following the American Standards and making no correction for daily variations in temperature. Such a correction can be made easily if desired. The purpose of making such a correction is to assure the same life base for all localities instead of a varying life base. In practice, it is not feasible to take full advantage of the present recommendations which allow increased loads for a departure from the base of 30 degrees daily average temperature and 40 degrees maximum temperature, since to do so would require daily adjustment of the load carried.

In most cases, where the daily ambient temperature is used for loading, the resultant aging will be less than for full load carried continuously with a constant ambient of 30 degrees. Consequently, it would not be inconsistent to use a constant 30-degree ambient as a reference base for calculating equivalent annual ambient temperatures and a 30-degree average with a maximum of 40 degrees for the daily ambient temperature.

With reference to Figure 3, the difference between the curves K'_8 and K_8 represents the temperature correction for range in daily temperatures during the month. This correction is equal to 2.2 degrees centigrade and corresponds to a range in average daily temperature during the month of 27 degrees centigrade, which is a typical value (from Figure 3 of reference 9 of the paper).

This paper and references 8 and 9 of the paper agree in the fundamental principles to be used in calculating equivalent ambient. These three papers differ in their application of the fundamental method, but there is no real inconsistency among them.

For convenience in calculation and analysis, as outlined in these papers, a satisfactory means of determining the equivalent ambient consists of the sum of three factors:

1. A mean temperature for the given period of time.
2. A correction factor based on the range during the given period of mean temperatures for the next smaller unit of time.
3. A correction factor based on the range of temperatures during the next smaller interval of time and so forth.

The base mean temperature is usually selected either for a year or for a month. This base mean temperature may be the average of several years or months, the maximum mean temperature over a long period of time, or some intermediate value.

The typical manner of variation of the temperature during the period of time considered can be determined theoretically, checked by empirical data. The range selected for determining the correction may be an average range for the whole country, an average range for that locality, the maximum range for that locality for the time considered, or some other value which would be typical. For convenience, any value so selected should be determinable readily from the records of the United States Weather Bureau.

The factors mentioned in the preceding paragraph apply, of course, for each succeeding smaller unit of time, until the hourly variation of temperatures during a day is reached. For the variation of temperatures during a day the thermal capacity of the transformer comes into play. For most transformers thermal capacity produces the same effect as does a reduction of the temperature range during one day to 80 per cent of its actual value.

These three papers have used the same fundamental principles to calculate values of equivalent ambient temperature which establish the upper and the lower limits of such equivalent temperatures. Equivalent temperatures based on average temperatures over a long period of time represent the lower limit of equivalent ambient temperature, since an examination of weather reports will show that there can be a succession of several years with monthly average temperatures during the hot months in excess of the long-time average temperature for these months. A succession of as many as ten years, in which the average monthly temperatures during the summer months exceed the long-time average temperature for that month, is not uncommon.

Temperatures derived in reference 9 of the paper are definitely on the safe side and represent the higher limit of equivalent ambient temperatures.

The spread which exists between the higher and lower limits is not unduly great. For the cities compared, the variation is between four and nine degrees centigrade.

Methods readily suggest themselves by which the values obtained in reference 9 can be reduced by taking a less pessimistic view of the probable temperatures which may occur. Similarly, the values given in the present paper could be increased to allow for the difference between the average mean temperatures and the maximum mean temperatures to be expected and, perhaps, in addition, for the daily range in temperature.

There is some advantage in having available equivalent monthly temperatures. If it were decided to use equivalent monthly ambient temperatures, the equivalent monthly ambient temperature could be calculated as the sum of the maximum monthly mean temperature, plus a correction for monthly range, in which the monthly range is assumed to be twice the difference between the maximum temperature ever recorded for that month, and the maximum monthly mean temperature. An alternative suggestion, which will give values somewhat more optimistic, would be to apply a monthly correction equal to the correction for a range equal to the difference between the maximum temperature recorded for the month and the maximum monthly mean temperature. This correction would be applied twice, once as the monthly correction, and once as the daily correction. It would be based on the assumption that one half of the variation was due to variation in hourly temperatures during the day. This correction would tend to be slightly optimistic, because the correction for range would be based on the most favorable conditions which are equal division of the temperature range, but this would be compensated for to some extent by the use of the maximum mean temperature ever recorded for the month.

The establishment of maximum and minimum limits for equivalent annual ambient temperatures with a small spread between them indicates the progress which has been made in the determination of equivalent ambient temperatures for overloading transformers. When the nature of the problem is considered, the agreement obtained between optimistic and pessimistic values should be reassuring to those who plan to use equivalent ambient temperatures for transformer overloading.

R. Rüdenberg (Harvard University, Cambridge, Mass.): From the standpoint of the transformer, it is very important to determine the maximum permissible temperature, or temperature rise, with corresponding limit to which the load may increase without undue deterioration of the insulation. However, if I were a transformer operator during this wartime, I probably would approach the problem from a quite different and much less conservative viewpoint.

Since most of the transformers in our networks have a life expectancy of the order of ten years or more, whereas the war and, thus, the demand for high industrial loads will last probably for a period of five years or less, I would sacrifice quite a number of years of the future life of the transformer by overloading it heavily, if this should be required by the conditions of war industry. I then would sell the power exceeding the well-established rated load of the transformer at a higher price per kilowatt-hour, derived from the more rapid deterioration of the insulation and the corresponding higher rate of depreciation. The increase in cost per kilowatt-hour would be very moderate, and I believe that the war industry would be perfectly willing to pay such extra cost as a return for getting the excess power at all. The additional money that would be received I would put aside until the end of the war, and then with it I would buy a new transformer, thereby solving another problem, namely, giving our transformer factories a good number of orders in the first period after the end of the war, when the returning labor needs it most.

I see no reason to prevent the application of a similar scheme for turbine generators, if their overloading should be seriously required, although the cost balance may not be so favorable here.

C. F. Wagner (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The ultimate purpose of determining the annual equivalent ambient temperature is to permit loading beyond the rating of the transformer when this quantity is below 30 degrees centigrade. It is highly desirable that any rule for this purpose should be as simple as possible. The rules proposed to date, in an effort to obtain accuracy, have suggested procedures that are too complicated, and the accuracy implied is not warranted by fundamental considerations of the problem. In the first place, the eight-degree rule for insulation aging is accepted without question, whereas the data do not justify this

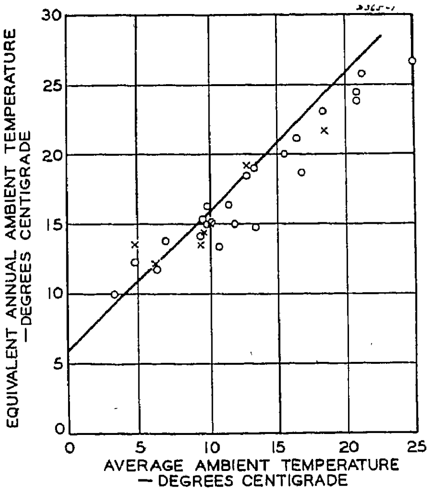


Figure 1. Comparison of Oldacre's equivalent ambient temperatures with proposed rule

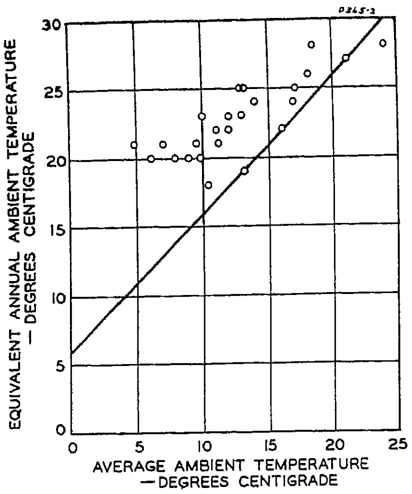


Figure 2. Comparison of Sealey's equivalent ambient temperatures with proposed rule (straight line)

faith. Insulation experts are far from agreement in their views regarding the factors affecting aging as to whether insulation aging follows an eight-degree rule, or a five-degree rule, or does not age appreciably until a certain critical temperature is reached. It is likely that the laws change with the character of the transformer, whether or not oxygen is permitted to come in contact with the oil. Also, the effects of other factors, such as the location of the transformer with respect to reception of direct sunlight, whether it be exposed or protected, are not considered. Further, it is assumed that the loading of the transformer is constant.

In Figure 1 of this discussion the equivalent annual ambient temperatures obtained by Mr. Oldacre are plotted as circles against the average ambient temperatures. The points represented by the crosses were obtained from the data contained in the Hellmund and McAuley[1] paper corrected to normal as indicated in Table III of Oldacre's paper. The writer is proposing that the values given by the straight line be used. This is a simple relation that is obtained merely by adding six degrees to the average ambient temperature. By this proposal the equivalent annual ambient temperature would be equal to the average annual ambient temperature plus six degrees. In view of the indefinite character of the assumptions underlying this work, it is the writer's opinion that the proposed rule is sufficiently accurate for all practical purposes and possesses the important advantage of simplicity.

Similar data have been plotted from Mr. Sealey's paper.[2] These points lie above the suggested curve, because they are based on maximum variations and are probably too conservative. The disparity between the Oldacre and Sealey values of equivalent ambient temperature is further evidence of the unsettled thinking with regard to this problem. It constitutes a further argument for not requiring accuracy of high order.

This proposal is equivalent to assuming that K_8' of Figure 3 of the paper is equal to 6.0. While this may appear to be a rather rough approximation, the proposal should be judged in terms of the total correction to be applied to the rating of the transformer; that is, the accuracy of the equivalent ambient temperature should be judged in terms of the amount by which it lies below 30 degrees centigrade.

REFERENCES

1. APPLICATION OF APPARATUS AND CONDUCTORS UNDER VARIOUS AMBIENT-TEMPERATURE CONDITIONS, R. G. Hellmund, P. H. McAuley. AIEE TRANSACTIONS, volume 61, 1942, August section, pages 553–8.

2. EQUIVALENT AMBIENT TEMPERATURES FOR LOADING TRANSFORMERS, W. C. Sealey. AIEE TRANSACTIONS, volume 62, 1943, February section, pages 87–93.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): I wish to discuss this paper first from the standpoint of comparing M. S. Oldacre's derived equivalent annual ambient temperatures with those obtained by W. C. Sealey in his paper (reference 9 of M. S. Oldacre's paper).

When the ambient temperatures derived from six cities, Bismarck, Helena, Kansas City, Denver, Dallas, and Chicago, are compared, it will be found that Mr. Oldacre's data permit an average of 7.0 per cent greater overloads than Mr. Sealey's data permit. The difference, of course, is due to the fact that Mr. Sealey was very conservative and used the greatest mean temperature recorded over several years, whereas Mr. Oldacre used annual average values.

We, therefore, have two papers whose authors differ in their ideas of how recorded ambient temperatures should be used in deriving equivalent annual ambient temperatures. Both are right, depending on the degree of conservatism one wishes to use.

As I see the situation, we are supposed to take full advantage of any and all factors during the war either to conserve critical materials or to get more capacity out of existing transformers, and for this reason I think that Mr. Oldacre was justified in using the recorded ambient data as he did. Perhaps after the war we may wish to be more conservative and use Mr. Sealey's method of deriving the equivalent ambient temperatures.

The question of whether we should use daily average or daily equivalent ambient temperatures in applying the one per cent rule for overloading in cool ambients naturally arises. Both Mr. Oldacre's and Mr. Sealey's curves show that for a change of 20 degrees centigrade daily temperature (which represents a good average value) the effective ambient is in the order of one to two degrees centigrade higher than the average ambient. The one per cent rule has a small margin of safety for most transformers, that is, if a self-cooled transformer has 45 degrees centigrade top-oil rise, 65 degrees centigrade hot-spot rise, and a 2:1 loss ratio at rated load, for 20 degrees centigrade ambient it could carry 111.5 per cent load for a 95-degree hot-spot temperature instead of 110 per cent load permitted by the 1 per cent rule, 121.5 per cent load instead of 120 per cent load for 10 degrees centigrade ambient temperature, and 132 per cent instead of 130 per cent load for zero degrees centigrade ambient temperature. For this reason it is not really necessary to use equivalent daily ambient, but rather average daily ambient temperatures, when applying the one per cent rule.

Under most conditions a greater benefit can be derived probably from using the annual equivalent ambients rather than daily average ambient temperatures. Where overloads are required during the winter months only, the one per cent rule used with daily ambient temperatures would be better than the one per cent rule used with the equivalent annual ambient temperature. It is obvious that both rules cannot be used. The user, however, should be able to decide which rule, that is, the one per cent rule used with daily average ambient temperatures or the one per cent rule used with the equivalent annual ambient rule, works out best for his particular loading condition.

M. S. Oldacre: Mr. Sealey points to the method used in the paper as obtaining "average equivalent ambient temperatures based on an average year." This statement, which he develops in his discussion, is not correct. The equivalent temperatures obtained by methods in the paper are based on all the recorded temperatures over a long period of time and are not based on either average temperatures or extreme temperatures. The approximate method outlined in the paper uses the average temperatures, since they are readily available, but makes correction which takes into account the variations from averages that occur over a long period of time. This seems to be a better procedure than to use only averages or only extremes.

Mr. Wagner's suggestion to add six degrees centigrade to the average annual temperature is a very rough approximation which is good enough for some localities but not very adequate when there is either a great or small range of temperature during the year. As shown in Figure 3 of my paper, the correction factor is not a constant but a function of the temperature range. The use of the K_8' curve of Figure 3 gives a reasonably accurate value of equivalent annual ambient temperature from readily available United States Weather Bureau data.

As pointed out by Mr. Montsinger, we believe that the use of the equivalent ambient temperature, as derived in this paper, has a sufficient degree of conservatism for present-day conditions.

Thermal Rating of Overhead Line Wire

Discussion of paper 43-73 by Myron Zucker, presented at the AIEE North Eastern District meeting, Pittsfield, Mass., April 8–9, 1943, and published in AIEE TRANSACTIONS, volume 62, 1943, July section, pages 501–07.

Jerome J. Taylor (Detroit Edison Company, Detroit, Mich.): It is readily apparent that emergency overload ratings are valuable in application to overhead lines, as they are to transformers, circuit breakers, and other equipment. It is not so apparent that running line wire up to temperatures of 90 degrees centigrade or more, continuously, as inferred by the author's Figure 13, is either good engineering or a net service to the war effort.

If 3/0 copper conductor operated at 500 amperes is taken, as shown in the mid-range of Figure 13, the I^2R loss per mile of three-phase line is about 325 kw, four or five times that which occurs at ordinary loadings. At 500 amperes, one quarter of the total energy is thrown away in 17 miles of 24-kv line, or in 85 miles of 120-kv construction. Stated another way, the wastage of coal is about 300 tons per day per 100 miles of line thus operated. Losses of this magnitude seem to call for considerable justifying, even though the capacity of the lines has thereby been approximately doubled. Another drawback is that under such conditions there is little margin left for emergencies.

From the other side of the picture, if we assume that high loadings are advantageous in particular cases, reinforced conductors such as steel-reinforced aluminum cable enjoy large advantages over hard-drawn copper in regard both to strength and to sag considerations, since steel is little affected by relatively high temperatures. There is a good deal of such construction in service. The paper in limiting its consideration to copper only is more specialized than is indicated by its title.

In regard to a minor point, the writer questions the validity of the author's statement that ". . . . (self-) circulation was more effective, per unit speed in carrying away heat than equal real wind speeds." There seems no basis for such a conclusion either experimentally or theoretically. The difference in slope between the no-wind and the wind curves, of which Figure 6 is an example, is adequately explained by the fact that the air velocity of self-circulation increases with temperature rise of the wire, whereas external wind velocity does not.

The Sorocabana Railway Electrification

Discussion of paper 43-67 by Durval Muylaert, presented at the AIEE North Eastern District meeting, Pittsfield, Mass., April 8–9, 1943, and published in AIEE TRANSACTIONS, volume 62, 1943, pages 804–18.

Sidney Withington (New York, New Haven, and Hartford Railroad Company, New Haven, Conn.): This paper is exceedingly interesting in the data it presents, especially in indicating the status of railroading in a part of the world remote from the United States. The high cost of fuel and the low cost of labor in that section of Brazil are a surprise to us in the United States. A locomotive maintenance bill of 3.1 cents per mile and a fuel cost of $1.48 would mystify some of our master mechanics mightily, who report maintenance expense of six times, and fuel cost of one sixth of those figures. Skilled labor at 12 cents per hour would indeed make them envious.

The figure of more than 50 watt-hours per trailing ton-mile seems high compared with our figures, even considering the fact that the locomotive weights are relatively high compared with trailing tonnage. It would be of interest to know just why. In a Paulista report of some years ago 21.3 for freight and 35 for passenger were given.

The power costs are of interest: 90 cents per kilowatt per month demand, and energy cost of just over one mil—total cost being less than half a cent, with a load factor of less than 40 per cent. It is easy to see why the feeding back of surplus regenerated power was not thought necessary, even in the small net amounts it is contemplated will exist.

In this connection it is intimated that regeneration is not easily obtained in single-phase systems. It may be pointed out that there is considerable dynamic braking on some railroads abroad with single-phase motors where, as in Brazil, energy from water power is cheap.

The railroad is to be congratulated in resisting the temptation to cover modestly the locomotive running gear with petticoats. In spite of the alleged popularity of such efforts in this country, especially with steam locomotives, streamlining equipment thus does not seem justified.

The concrete hollow poles for catenary are of interest. It would be interesting to know just how the core or mandrel is designed in casting. Also it would be interesting to learn how the poles are climbed in maintenance.

It is a little harsh to charge engineers with prejudice in the design of catenary structures. There is no doubt that most of our designers would conscientiously adopt the most economic design, regardless of personal preference; the difficult line to draw is that between cheapness of construction and expense of maintenance and reliability of operation. That is a most vexing problem.

It is understood that the power in the vicinity of Rio de Janeiro is 50 cycles. It would be interesting to know just the division between 50 and 60 cycles in Brazilian territory.

Overvoltage Protection of Current-Transformer Secondary Windings and Associated Circuits

Discussion and author's closure of paper 43-70 by R. H. Kaufmann and G. Camilli, presented at the AIEE North Eastern District technical meeting, Pittsfield, Mass., April 8–9, 1943, and published in AIEE TRANSACTIONS, 1943, July section, pages 467–72.

C. A. Woods, Jr. (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The subject of the paper is very timely in focusing the attention of engineers upon a source of overvoltages in the secondary circuits of current transformers due to the rapid increase in installations involving the switching of lumped capacitance on power systems. The test data and calculations show that, under certain conditions, excessive voltages may be developed in the secondary circuits of current transformers used in such installations. The data given show very clearly the factors, such as source voltage, intervening line impedance, and current-transformer ratio, which determine the magnitude of the overvoltages induced in the current-transformer secondary circuits. This information should be of considerable benefit in determining whether any additional precautions regarding the protection or installation of transformer secondary circuits are necessary in such instances.

As stated by the authors of the paper, there are other sources of overvoltage on the current-transformer secondary circuits which have been recognized for many years. Likewise, in the past there has been considerable use made of some form of protective device, such as protective gaps, protective tubes, or other similar devices, for short-circuiting the secondary circuits of the current transformers when overvoltages occur, where these transformers were used for operation of instruments. On the other hand, there has been considerable hesitancy on the part of the industry to utilize any such device on current-transformer secondary circuits where the transformers are used to energize protective relays. The principal reason for this is that any such device has rendered the relays inoperative at the very time when they are most likely to be called upon to operate.

The new protector described herein seems to overcome the principal objection to the use of overvoltage protectors on the secondary circuits of current transformers used to energize relays. The curves of Figure 6 of the paper show the device performance under a specific set of conditions, but it would be interesting to know the volt-ampere characteristics of the protector, thus providing an indication of the limitations under varying conditions of currents and burdens.

We have developed recently a protective device, in combination with multicircuit arrangements, which will restrict the overvoltages in the secondary circuit of current transformers and still permit operation of protective relays. This device consists of protective gaps, tubes, or similar devices in series with an impedance, both in shunt with the secondary burden, and located as close to the secondary terminals of the current transformer as possible. The series impedance is co-ordinated with the burden and relay application so as to permit a proportional amount of the secondary current to flow through the burden because of the voltage drop across the impedance, even though the protective tube or gap has operated at the predetermined voltage. With this device in certain multiphase arrangements, particularly three-phase arrangements of current-transformer secondary circuits, it is even possible to obtain functioning of the protective relays, in certain instances restricted functioning, with one of the secondary lead wires or burdens open-circuited.

Over the years, operating experience has shown on average installations, capacitor installations for line power-factor correction excepted, that the ordinary overvoltages developed on current-transformer secondary circuits do not warrant the use of such protective devices in circuits involving protective relays, except under unusual conditions. Such unusual conditions involve very high ratio transformers, very long and possibly exposed secondary leads, and current transformers having unusually large cross-sectional area of core. Where the unusual conditions justify the application of such a device, it seems desirable that it should permit, to as great a degree as possible, proper functioning of protective relays.

The paper under discussion clearly indicates another special condition, wherein current transformers that are installed in, or adjacent to, circuits containing lumped capacitance require an investigation of the possible overvoltages on the secondary circuit. If it is found necessary, suitable protective measures should be taken.

M. C. Westrate (Commonwealth and Southern Corporation, Jackson, Mich.): After reading this paper, one is impressed with the need for overvoltage protection on current-transformer secondary circuits. However, the question immediately arises as to why there has not been more trouble experienced without such protective devices. The examples and tables in the paper show conditions where capacitors are involved, and it may be that high voltages will be experienced with increased capacitor installations. In the paragraph headed "Need for Overvoltage Protection," the authors do not limit the application of the protective devices to capacitor installations.

In the discussion of open-circuited secondary circuits, the authors mentioned that the voltage magnitude is a function of the current-transformer magnetic design. It would seem as though the saturation of the iron would be a very definite factor limiting the maximum voltage. Bushing-type current transformers, in particular, would be subject to the effect of saturation. It would be of interest if the authors would supply additional data or suitable references on the effect of saturation.

G. Camilli: Mr. Woods has amplified on the scope of the paper and has stressed the fact that the new protector does not interfere with the operation of overcurrent relays.

While the curves of Figure 6 of the paper show the performance of the device with a Z burden connected across the secondary circuit of the current transformer, the flexibility of the device is such that any predetermined characteristic can be obtained easily.

Mr. Westrate's comments are well founded. There are many cases in which the circuit insulation has been punctured by excessive transient voltage which may not be evident at normal rated current.

Automatic protection of every current transformer against open secondary circuits is desirable if the cost and size of the device are relatively small. There are, however, some applications where protection certainly should be considered, for example, high primary-current ratings, low secondary-current ratings, applications to test circuit where connections may be changed frequently.

New Developments in Potential-Transformer Design

Discussion of paper 43-71 by G. Camilli, presented at the AIEE North Eastern District meeting, Pittsfield, Mass., April 8–9, 1943, and published in AIEE TRANSACTIONS, volume 62, 1943, July section, pages 483–7.

J. J. Samson (Commonwealth Edison Company, Chicago, Ill.): The new type of potential transformers described by Mr. Camilli is quite an innovation, as it includes in one unit several new developments of the transformer industry. Most developments are rather piecemeal in using new methods and processes, and it is quite refreshing to see so many changes at one time.

In addition to the saving in materials, there is an improved insulation structure, practical elimination of gaskets by welding of cover to case and by sealed construction between porcelain and metal parts of the bushings, and also the use of noninflammable insulating liquid. All these points are included, and at the same time the accuracy is said to be as good or better than the older designs.

From the application angle, the new design of potential transformer has made long strides toward the 100 per cent we are looking for. Obviously, the smaller and lighter the transformer can be made, particularly if the size will allow mounting on the bus structure, the greater the saving in labor, material, and over-all area otherwise required. The material saving above 46 kv should be appreciable.

The new design also promises to mitigate some of the other potential-transformer problems. The one always present is that of protection—not of the potential transformer itself but of the bus, its associated equipment, and the service it supplies—from the effects of faults in the potential transformers.

A "stewing" fault in the secondary or the ground end of the primary winding of large oil-filled potential transformers is a major hazard. Fusing the primary winding is the usual remedy, with its well-known "headaches." If the fuse current rating is high enough to withstand the inrush current due to switching surges and other transients, it will not protect for faults in the secondary and in the ground end of the primary winding (because of the necessary high impedance of the primary winding). If the current rating is low enough to protect for these faults, it becomes a hazard to the relay protection. For example, a 138-kv potential transformer would require a 0.25-ampere fuse to protect for secondary short-circuits. The fuse will blow on surges or momentary secondary short-circuits, removing the potential from the reverse-power or impedance relays. It is therefore a step in the right direction to design the transformer so that the probabilities of fault are reduced to a point where the protection is not considered necessary.

It would have been helpful if more detailed description had been given of the joint between the porcelain and metal supports. This is an important development that is quite new, and, although it might be the subject of a separate paper, more information would be valuable with Mr. Camilli's paper.

The complete sealing of the transformer by welding and solder-sealed bushings will eliminate gaskets and greatly reduce the chances of trouble with the insulating liquid and insulation since moisture and oxygen are excluded. The improvements in shielding and impulse strength, together with proper lightning-arrester protection, should practically eliminate the chances of faults due to lightning. The small amount of liquid or the use of Askerel as liquid will make the fire and explosion hazard negligible.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): Mr. Camilli has done an excellent job in designing and building potential transformers of a construction so nearly the ideal. The paper states in general terms how the great reduction in size and weights was accomplished but gives no data on the relative strengths of the conventional method of arrangement of insulations and the arrangement which was used.

Two factors that appear to contribute to the improvement made in these potential transformers are:

1. Eliminating as far as possible nonuniform dielectric fields.
2. Eliminating oil in series with solid insulation between the high-voltage and low-voltage windings.

With reference to the first point: it can be stated that for nonuniform dielectric fields the spacing varies approximately as the three halves power of the voltage, that is $kv^{1.5}$, whereas for uniform fields the spacing varies approximately as the first power of the voltage. For low-voltage designs this factor is not very important, but for high-voltage designs it becomes very important. For example: if we assume that it requires the same insulation distance for both uniform and nonuniform fields for a test of 70 kv, then for a test of 460 kv the distance for the nonuniform field would be $(460/70)^{1.5}=16.8$ times that for the 70-kv test, whereas for the uniform field the distance would be $460/70=6.57$ times that for the 70-kv test or approximately four tenths of the space required for the nonuniform field.

On the second point: the benefits to be gained by elimination of the oil ducts in series with the insulation barrier between windings is best illustrated by an example, based on the fact that the division of voltage across solid and oil in series is inversely proportional to their dielectric constants. The dielectric constant of solid insulation is approximately twice that for oil.

The following assumptions are made:

1. The dielectric strength of oil is 200 volts per mil (uniform field).
2. The dielectric strength of solid is 250 volts per mil.
3. Test = 300 kv

If all solid is used, the thickness of barrier will be $300/250=1.2$ inches.

Half of barrier is now oil and half is solid. The stress on the oil is of course twice the stress on the solid, because of the ratio of their dielectric constants. Therefore, only half of the solid can be added to the oil to get the equivalent oil spacing required.

The space required will be $300/200=1.5$ inches of equivalent oil space made up of one inch of oil and one inch of solid, or a total thickness of two inches which is 1.67 times the thickness of the 1.2-inch solid barrier.

In other words, by the elimination of free oil ducts the barrier need be only 0.6 of that for a barrier with free oil ducts. Also, it was shown that with a uniform field the space was reduced to approximately 0.40 of that for a nonuniform field. The product of the two reductions is $0.6\times0.4=0.24$ per cent, or one quarter of the space between high-voltage and low-voltage windings for the conventional design.

A third factor which resulted in the saving of space was the ingenious method used in folding the paper over the ends of the layers in the high-voltage winding, which thereby eliminated large creepage distances ordinarily required in high-voltage layer-wound coils.

Automatic Voltage Compensator for Resistance Welding Control

Discussion and authors' closure of paper 43-121 by E. M. Callender and R. S. Phair, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 701–05.

Benjamin Cooper (General Electric Company, Schenectady, N. Y.): The work presented in this paper represents a step in the advancement of high-quality resistance welding as applied on a mass-production basis. The problem of either providing a heavy electric-power distribution system or providing electrical apparatus to compensate for the voltage variations which are unavoidable on less-substantial power systems, is a problem for users of resistance-welding equipment if they are to maintain consistently high-quality welding, particularly on power systems that become more and more overtaxed.

While the first alternative of building a power-supply system with low-voltage drop represents a very sound way of improving the voltage conditions, it may not be selected for at least two reasons: First, it may not represent the most economical solution; and second, in view of our wartime emergency, the new aspect of a maximum saving in critical materials becomes an important item for consideration.

If the building of a heavier power system were selected as the way of improving the voltage conditions, anticipated growth would be the deciding factor as to how much more substantial the power system should be made. Obviously, an adequate system today may soon be overtaxed by an unanticipated growth. This, then, means unused copper and idle fixed assets today in anticipation of tomorrow's growth. On the other hand, the voltage-compensating apparatus may be added as an integral part of the new installations, or, in many cases, used to supplement the older installations.

One form of electrical apparatus which is now commercially available and which may alleviate the poor-voltage conditions at the welder is the capacitor for power-factor improvement. This type of apparatus reduces the voltage variations by reducing the high reactive demands such as are required by a welding transformer. However, in some cases it may be either impractical or uneconomical to add capacitors for purposes of improving the voltage conditions. The best answer in these latter cases would certainly be the voltage-compensating or current-regulating control. Then too, there is the possibility, even after capacitors have been added, that the electrical demand may be great enough to require the voltage-compensating control anyway. The voltage-compensating control is an electronic device which does not attempt to correct the voltage variations, but rather changes the current by phase-control methods so as to provide a constant welding current irrespective of these voltage variations. While in the authors' case, their control is designed to compensate for voltage variations, it should be pointed out that electronic type of control is not necessarily limited to compensating for voltage variations alone. Electronic control may be made to regulate the welding current and to compensate for changes in the welding-transformer power factor, impedance, or any combination of voltage and impedance effects that might change the welding current. This is an advantage which cannot be gained in any way except by means of proper control. There are in operation numerous current-regulating compensators which permit a constant welding current to be delivered to the weld, irrespective of the size of the welder throat or of the change in projected metal into the throat of the welder. One example is an arrangement where identical welds are made alternately, using first the main electrodes of the welding machine and then an auxiliary parallel-connected welding gun without making any changes in transformer taps or current adjustments on the welding control.

In the authors' case, because they were welding nonmagnetic materials they did not experience any appreciable impedance changes; hence they were interested only in voltage compensation. For their work, a special voltage compensator was used advantageously in conjunction with the current squared–time or I^2T recorder, the I^2T recorder being a standard part of each welding control operated by them. Using the voltage compensator in these systems, they have demonstrated conclusively, by actual operations, that high-quality welding can be maintained in production lines, independent of the line-voltage variations even though, at times, these variations may be in excess of 20 per cent.

To produce equivalent-quality welding by addition of extra feeders would have required that the capacity of the power system be more than doubled. This is a striking enough comparison.

In conclusion, when the abilities of electronic control, as a device for maintaining constant welding current, become more common knowledge through industrial experiences and papers such as this, there is little doubt but that the future may find an electronic regulating unit an integral part of most welding installations.

E. M. Callender and R. S. Phair: Mr. B. Cooper's discussion brought out a consideration that was not previously stressed. Certain types of welding, notably seam welding, may require special control when welding material having permeability greater than unity. For instance, the configuration, or otherwise the varying portions of the work, passing through the throat of the welder loop may have a marked effect on the secondary-circuit reactance. The authors did not meet particularly with this problem, since the welding material is mostly stainless steel with permeability approaching unity.

Constant-current regulators which regulate the current for many of the causes, such as line-voltage drop or reactance change, are understood to be available. These devices are somewhat more elaborate and probably require more attention than the automatic voltage-compensator described.

We wish to emphasize that the voltage compensator in no way affects the regulation of line voltage, but regulates the welding current during the multiplicity of machine operation on the same line. Therefore, full consideration still is required to determine that the application contemplated will not reflect undue voltage drops back to the power company high-voltage line, with consequent light-flicker problems. Multiple-welding installations require as "stiff" a source of power as possible and complete divorcement of lighting and welding loads within the plant.

The authors appreciate Mr. Cooper's clear interpretation of plant savings in copper bus feeders through use of the voltage compensator.

A Study of Voltage Transients in Arc-Furnace Circuits

Discussion and author's closure of paper 43-97 by J. B. Hodtum and J. B. Rice, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 556–62.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The authors have found in their study that the highest switching surge was of the order of 5.2 times normal. This corresponds to approximately 73 per cent of the full-wave impulse test level of the rated transformer voltage. From tests made on barriers representing transformer insulation, such barriers can withstand switching surges of approximately the same amplitude as the full wave. Furthermore, even though switching occurs rather frequently, from the data presented it appears that the highest surge could occur only once in approximately a month's operation. These results do not differ greatly from those obtained at other installations.

The authors also have found that the surges originating on the low-voltage side, due to arcing, are not transmitted to the high-voltage terminals. This also has been confirmed by others. However, tests made by the General Electric Company show that such transients are transmitted to the high-voltage side of the transformer. In Figure 1 of this discussion, upper trace, is shown the voltage to ground at the junction of the high-voltage winding and the internal reactor. The violent changes in voltage and the high-frequency character are of similar nature as the surges on the low-voltage side. The lower oscillogram trace shows the simultaneous voltage to ground from the other end of the transformer winding which is connected to the supply line and shows no disturbance whatever.

The installation on which these oscillograms were obtained is similar to the ones described in the paper, except that the high-

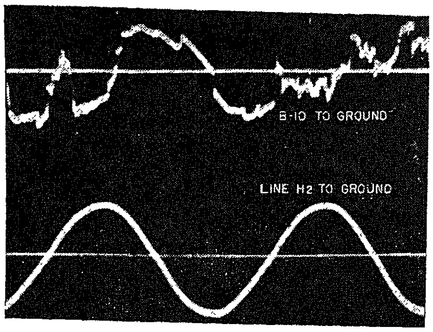


Figure 1. Line voltage (H_2 to ground) and voltage from reactor–transformer connection (*B*-10 to ground)

Rapid rate of voltage change inside of transformer–reactor windings is not associated with disturbance on the transformer line terminals

voltage winding was rated 13,800 volts, delta–wye. The network feeding this transformer is a 13.8-kv cable network of 52 miles total cable length. Power to this network is supplied principally from a 115-kv system and partly from generators through step-up transformers.

Measurements made with surge-voltage recorders from line to ground showed overvoltages of less than four times normal line-to-ground voltage crest. These overvoltages occurred only during switching.

Figure 2 of this discussion, upper curve, shows the voltage from transformer reactor junction to ground with peak voltages of about two times normal line-to-ground voltage crest and a frequency of approximately 10,000 cycles. In the lower curve the associated arc voltage and current show that the violent changes on the transformer high-voltage side are associated with rapid changes in arc current.

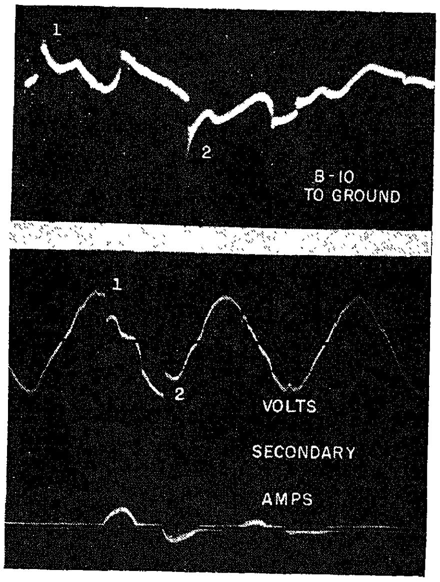


Figure 2. Transients within high-voltage windings are caused by arc phenomena in the furnace

Top. Voltage to ground from transformer–reactor junction

Bottom. Arc voltage and arc current. Points 1 and 2 relate voltages at high-voltage and low-voltage terminals

In this study it was found that only in the melt-down period such high-frequency transients occur in the high-voltage winding of the transformer. The frequencies of the observed transients ranged between 10,000 and a few-hundred cycles. The maximum voltages measured across the reactor and across the high-voltage winding of the transformer with surge-voltage recorder were only of the order of 2.5 times to 3.5 times normal line-to-ground voltage crest. However, in cases of resonance between natural periods and applied frequencies, it appears possible that voltages of much higher relative amplitude may appear across certain portions of the windings. These high-frequency surges are very numerous. Thirty-three of the steep-front 10,000-cycle surges of Figure 2 occurred within the observed period of 48 cycles. Therefore, it would appear that the internal transient voltages caused by the low-voltage arc transients are much more important from the point of view of transformer insulation than the relatively infrequent switching surges.

C. C. Levy (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): It seems to me that in some cases the conclusions drawn are too positive, and not wholly justified by the evidence; for example, I do not believe that the statement made by the authors to the effect that characteristics of the electrode control have an important bearing upon voltage surges caused by instability of the arc is justified. In the body of the paper evidence is submitted that voltage surges do not occur when the secondary circuit is opened, and this of course is what would happen if the arc were unstable.

Again, I do not think that sufficient evidence is produced to reach the conclusion that tripping under load in the early part of the heat will result in higher-voltage transients than load tripping during refining. This is merely an academic consideration, because in the refining period load is quite stable, and such short circuits as occur are rapidly cleared by the regulator so that the breaker seldom, if ever, opens during this period. My theory is that if there is a difference, as the authors claim, it will probably lie in the fact that simultaneous extinction of the arc on the secondary side, caused by regulator operation, and the opening of the breaker on the primary side is much more likely to happen during the melt-down period than during refining. This condition would give rise to higher transients during meltdown.

I should like to ask Mr. Hodtum if the tests at location *A* when the change was made from cable to overhead line showed greater number as well as greater magnitude of the surges for the cable as against the overhead line. It is also possible to assume that the change in overvoltages when the cable was replaced by overhead line might have been due to arcing grounds in the cable system. I mention this because some recent tests on a furnace circuit supplied by a cable showed a large number of overvoltages which were proved definitely to be caused by an arcing ground in the cable system.

I should also like to ask whether the possibility may not exist that closing surges are attributable in some cases to the mechanical condition of the breaker causing restrikes in closing.

G. E. Shaad (nonmember, General Electric Company, Schenectady, N. Y.): There are some unknown factors in regard to the circuit characteristics used in these tests when it comes to explaining the high voltages obtained, and a great deal of study may have to be given before all of the phenomena definitely can be explained. For instance, an "ideal" breaker might be considered as one which closes all three phases at exactly the same instant; there is no restriking phenomenon on either closing or opening, and the current is interrupted at normal current zero. It is well-known that for such breaker operation it is not possible to get over twice-normal voltage on the closing or opening of a circuit irrespective of the circuit constants.

When we necessarily discard the desirable characteristics of the "ideal" breaker, however, it is theoretically possible to get transient voltages many times normal, during both opening and closing of transformer circuits. So little is known, however, in regard to the circuit-breaker phenomena present in these tests that we should not be too hasty to condemn any one item, for example, the use of cables on such installations. For instance, it is interesting to note the following facts in regard to the tests just completed and presented.

It may be noted that in Figure 5 of the paper, curve *B*, a maximum of four times normal voltage was obtained on a grounded system with *no* underground cable, while in Figure 6, curve *B*, exactly the same value of four times normal voltage is given as a maximum for a grounded system with cable. Thus, there would seem to be little to choose between the two.

The capacitance of cable is some 15 to 20 times that of overhead transmission lines. In the particular case used in this paper the cable run was 650 feet. Thus, it was equivalent in capacitance to about $2^1/_2$ miles of overhead transmission line. It is interesting to note that there are some 350 circuit miles of overhead transmission in this system. Thus, the capacitance of the cable involved adds very little to that of the system.

Switching surges of four or five times normal, caused by restriking of the arc in the interrupter, have been recognized for many years, and it is believed that adequate protection against contingencies of such overvoltages should be provided, as may be obtained by the use of protective equipment, instead of changing the form of one of the circuit elements, which may be inconvenient and is of doubtful value.

It would appear, therefore, that the excellent data presented in this paper should serve as the groundwork for future study. The fact still remains that there are hundreds of successful installations using cable.

Sherwin H. Wright (Ebasco Services, Inc., New York, N. Y.): In the paper it is stated that it appears unlikely that overvoltages caused by breaking magnetizing current should be a source of serious concern. One's attitude toward the possibility of overvoltages from breaking magnetizing current depends on whether the overvoltages have actually been experienced. It is believed that breaking magnetizing current on power transformers not only can but does occasionally produce serious overvoltages on parts of systems, and that it should not be assumed that this is unlikely to be of serious concern. It may occur only rarely, but when it does occur it may create results which are quite serious.

Regarding overvoltages which are produced by switching, such as those mentioned in the paper as often occurring on the high-voltage terminals, it seems well worth-while to consider the possibility of their control by a resistor in the circuit-breaker interrupting element. This has already been accomplished on 230-kv circuit breakers used for extensive high-voltage transmission systems. Have the authors examined this possibility of control of transients?

H. G. Heath (Pittsburgh Lectromelt Furnace Corporation, Pittsburgh, Pa.): From tests and analysis of switching operations on arc-furnace installations, it has been known generally that voltage transients of considerable magnitude resulted. In fact, the first data obtained in connection with voltage transients on an arc-furnace circuit occasioned the observation that the magnitude was no greater than would be expected from previous knowledge of switching operations.

The present studies, I believe, should take into consideration the large number of furnaces in operation on which the insulation levels have been ample, at least to the extent that the few failures should be considered abnormal or exaggerated cases.

On 5-kv transformers the test voltage, high voltage to low voltage and ground, is 3.8 times the rated voltage, while I find it to be only 2.23 and 2.05 times, respectively, on 23 and 34.5-kv transformers, respectively. It has been the practice of my company to specify 34.5 kv installation test on 23 kv and 46-kv installation test on 34.5-kv transformers, which brings test values to 3 times and 2.7 times rated voltage, respectively. It is to be admitted that conditions are more severe on 23 and 34.5-kv installations and need to be given every consideration. There have been no failures on 13.8 kv where the ratio is 2.46 times the rated voltage, except in one or two abnormal cases.

On installation *A*, I was surprised at the mention of restrikes on the closing of the switching equipment. The switching equipment consists of an oil circuit breaker equipped with Ruptors where the current-carrying members consist of a bayonet and a segmented stationary contact equipped with springs to give sufficient pressure. The bayonet enters this stationary segmented contact to a degree that I can see no possibility of sufficient bounce to open or break contact. Possible explanation for this restrike indication is that it may be a reflected wave.

The use of surge capacitors has been recommended and tests made where they have been installed. The magnitude of the voltage transients has been reduced to approximately two times normal. Economically, I have found that in most cases it is less expensive to install surge capacitors than to insulate the transformer for the next higher-voltage class.

J. B. Hodtum: During the meltdown operation there is usually a reactor connected in series with the primary winding of the transformer. Between the secondary terminals of the transformer and the arc there is considerable reactance because of the secondary lead connections. For the refining period the primary reactor is usually removed from the circuit. If the primary switch is opened during the meltdown operation, with the electrodes short-circuited, the primary winding receives an inductive kick from the primary reactor and the secondary winding an inductive kick from the lead reactance. During operation of the furnace under this condition, oscillations appear at the junction of the primary reactor and the primary winding as indicated in Mr. Hagenguth's Figure 1, upper trace.

If the furnace arc interrupts with the primary breaker closed, similar disturbances occur at this junction.

Surge magnitudes of the order of 100 times normal have been recorded at the secondary terminals of the transformer by other observers. These probably resulted from the inductive kick produced by the collapse of the field surrounding the secondary leads when the primary breaker is opened and with the electrodes short-circuited.

During the refining period the primary reactance usually is removed from the circuit, and investigations conducted within the transformer winding indicate similar surges as shown by Mr. Hagenguth's figures.

Mr. Shaad raised the point regarding Figures 5 and 6 of the paper. Curve *A* of Figure 6 represents a system having an impedance ground and a lump capacitor on the line side of the breaker. Curve *B* represents a system solidly grounded, but with capacitance on the line side of the breaker. Curve *C* represents an impedance-grounded system without the additional capacitance on the line side of the breaker.

Curve *B* of Figure 5 represents a solidly grounded system without the additional capacitance. The magnitude and frequency for this connection is almost identical with that of curve *B* of Figure 6. The surges for curve *B* of Figure 6 are all on closing, while those of curve *B* of Figure 5 are equally on opening and closing.

The authors may be in error in assigning the principal cause of the overvoltages to the extra capacitance; however, when this capacitance was removed and with the system impedance grounded, the magnitude of surging was reduced from 4 times normal to $2^{1}/_{2}$ times normal maximum.

Mr. Hagenguth's point regarding the impulse strength of the winding is more or less confirmed, in that there have been few winding failures caused by switching surges. Practically all of the difficulties have been caused by the flashover of associated equipment.

Regarding Mr. Levy's question as to whether when the change was made from cable to overhead line showed a greater number as well as a greater magnitude of surges for the cable against overhead line, I regret that at the present time I cannot give him a definite answer. I am not, however, willing to admit that cable failures at other locations have been due to arcing grounds within the cable. It is quite possible that the arcing grounds were the result rather than the cause.

Mr. Wright raises the question regarding a resistor in the breaker interrupting element. This was considered, of course, and some testing was accomplished, the results of which indicate a reduction in magnitude of the overvoltage.

It is difficult to visualize how the breaker described in Mr. Heath's comment can restrike on closing. The magnitude of the overvoltages, however, indicates that some such phenomena must be taking place. It is possible that a similar result is obtained when all of the phases do not close simultaneously.

Practical Calculation of Electrical Transients on Power Systems

Discussion and authors' closure of paper 43-98 by R. D. Evans and R. L. Witzke, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 690–6.

T. Y. Shen (Nonmember; American Gas and Electric Service Corporation, New York, N. Y.): Evans and Witzke have made an important contribution to the method of calculating transients on power systems. On frequent occasions we encounter generator-neutral-grounding or lightning-arrester-application problems in which we have to determine the magnitude of transient over voltage due to ground fault. The magnitude of these transient voltages can be amplified considerably if the fault is of the arcing type or if it consists of successive clearings and restrikings during circuit interruption. Calculations for these types of faults are usually lengthy and cumbersome if conventional mathematics are used. By using the method suggested in Evans and Witzke's paper, many mathematical manipulations are by-passed, and hence much time is saved. Furthermore, this method is well adapted to the needs of many electrical engineers who would prefer to attack the problem without resorting to differential equations or Heaviside operational calculus.

In applying the present method to practical computations, our experience from various calculations indicates that in many cases the zeros and slopes can be determined quite easily from the analytical expressions of the reactance or admittance function and its derivative. This results from the fact that many ordinary power circuits can be reduced and represented by a relatively simple network. For this reason the angular velocity, ω, corresponding to zero reactance (or infinite admittance) between the driving-point terminals can be determined by equating the impedance function to zero and solving for the respective ω's. Similarly, the angular velocity corresponding to zero admittance (or infinite reactance) between the driving-point terminals can be determined by equating the admittance function to zero and solving for the respective ω's. This method of finding ω requires the determination of the roots of an algebraic equation whose degree is equal to the number of internal zeros of the reactance function or admittance function. In the case of a complicated network, the equation will be of high degree and will not be practical to solve. Under such conditions, the cut-and-try or graphical method developed in the paper will undoubtedly be more suitable.

If a high-degree accuracy is desired in the calculation of the final network response, the slopes as well as the zeros should be determined with high accuracy. The slopes of the angular velocity-reactance (or angular velocity-admittance) curve can be determined more accurately from the first derivative of the reactance (or admittance) function. If the reactance (or admittance) between the driving-point terminals be expressed in the following form:

$$X=\frac{1}{B}=\frac{\lambda_1\omega^n+\lambda_2\omega^{n-2}+\lambda_3\omega^{n-4}\ldots.}{\beta_1\omega^m+\beta_2\omega^{m-2}+\beta_3\omega^{m-4}\ldots.}$$

where λ_1, λ_2, ... and β_1, β_2, ... are functions of the circuit parameters, the procedure for determining its derivative at zero impedance (or admittance) can further be simplified, because all terms involving the product of the numerator of the impedance (or admittance) function are equal to zero. Therefore

$$\left.\frac{dX}{d\omega}\right|_{\text{at } X=0}=\frac{1}{(\beta_1\omega^m+\beta_2\omega^{m-2}+\beta_3\omega^{m-4}\ldots.)^2}\times$$

$$(\beta_1\omega^m+\beta_2\omega^{m-2}\ldots.)\frac{d}{d\omega}[\lambda_1\omega^n+\lambda_2\omega^{n-2}\ldots.]-$$

$$(\lambda_1\omega^n+\lambda_2\omega^{n-2}\ldots.)\frac{d}{d\omega}[\beta_1\omega^m+\beta_2\omega^{m-2}\ldots.]$$

$$=\frac{1}{(\beta_1\omega^m+\beta_2\omega^{m-2}+\beta_3\omega^{m-3}\ldots.)}\times$$

$$\frac{d}{d\omega}[\lambda_1\omega^n+\lambda_2\omega^{n-2}+\lambda_3\omega^{n-4}\ldots.]$$

Similarly

$$\left.\frac{dB}{d\omega}\right|_{\text{at } B=0}=\frac{1}{(\lambda_1\omega^n+\lambda_2\omega^{n-2}+\lambda_3\omega^{n-4}\ldots.)}\times$$

$$\frac{d}{d\omega}[\beta_1\omega^m+\beta_2\omega^{m-2}+\beta_3\omega^{m-4}\ldots.]$$

In most of the practical cases, very little numerical computation is involved by using this method. One further advantage is that the computation may be done on a slide rule, whereas the calculations for the incremental method as suggested by the authors in their illustrative example must be carried out to five or six figures or more in order to obtain the same degree of accuracy. This is true, because in the authors' method of evaluating $\Delta X/\Delta\omega$ or $\Delta B/\Delta\omega$, the result depends upon finding the difference of two numbers which are approximately equal. The truth of this statement is clearly shown by the numerical example in the paper.

It might be of interest to show a numerical example illustrating our method of computing the transients due to a circuit change. Taking the same problem as in Table VI in the paper, the admittance function can be expressed by the succeeding equation.

$$B=\omega C_1-\frac{1}{\omega L_1}+\frac{1}{\dfrac{1}{\omega C_2}-\omega L_2}$$

$$=\frac{L_1L_2C_1C_2\omega^4-(L_1C_1+L_2C_2+L_1C_2)\omega^2+1}{L_1L_2C_2\omega^3-L_1\omega}$$

By substituting numerical values, thus

$$B=\frac{10^{-14}\omega^4-2.1\times10^{-7}\omega^2+1}{10^{-8}\omega^3-0.1\omega}$$

Set $B=0$ and solve for ω's

$\omega_a=2{,}700$ or $\omega_b=3{,}700$

$$\left.\frac{dB}{d\omega}\right|_{\text{at } B=0}=\frac{1}{10^{-8}\omega^3-0.1\omega}\times$$

$$\frac{d}{d\omega}\ (10^{-14}\omega^4-2.1\times10^{-7}\omega^2+1)$$

$$=\frac{4\times10^{-14}\omega^2-4.2\times10^{-7}}{10^{-8}\omega^2-0.1}$$

At $\omega_a=2{,}700$

$$\frac{dB}{d\omega_a}=0.474\times10^{-5}$$

At $\omega_b=3{,}700$

$$\frac{dB}{d\omega_b}=0.344\times10^{-5}$$

$$A_a=\frac{2}{2{,}700\times0.474\times10^{-5}}=156$$

$$A_b=\frac{2}{3{,}700\times0.344\times10^{-5}}=157$$

$$I(t)=2.65\cos\left(377t-\frac{\pi}{2}\right)$$

at $\omega=377$

$B=-0.0261$

Therefore

$$I(t)=\frac{2.65}{-j0.0261}\cos\left(377t-\frac{\pi}{2}\right)-$$

$$\frac{2{,}700\times377\times156\times2.65}{2{,}700^2-377^2}\cos 2{,}700\,t-$$

$$\frac{3{,}700\times377\times157\times2.65}{3{,}700^2-377^2}\cos 3{,}700\,t$$

$$=102\cos 377t-59\cos 2{,}700\,t-43\cos 3{,}700\,t$$

R. D. Evans and R. L. Witzke: Mr. Shen's discussion is a noteworthy supplement to our paper, as it illustrates an alternative method of obtaining the zeros and slopes. As pointed out in our paper, under "Extensions of the Method," ". . . it is possible and sometimes convenient to form the analytical expression for the impedance (or admittance) function and its derivative and to use the first or both of these to determine the zeros and slopes." Mr. Shen has mentioned that it may be more convenient to obtain the zeros by graphical or cut-and-try methods when the equations involved are of a high degree. This procedure will also be useful, if not necessary, in those cases where hyperbolic or trigonometric functions are used to represent distributed circuit constants. In many cases we have found it desirable to calculate the zeros by graphical methods, and then to obtain the slopes by substituting the zeros in the general analytical expression for the derivative of the reactance (or susceptance) function. The procedure to follow in any particular case will depend upon the complexity of the network as well as the experience of the operator.

Mr. Shen's simplified expressions for the slopes of the reactance and susceptance functions are very useful when the complete analytical solution is made. Similar expressions can be obtained directly from the Heaviside expansion theorem as follows:

$$i(t)=i_0(t)+\sum_{k=1}^{k=2n}\frac{e^{p_kt}}{p_kZ'(p_k)}\tag{1}$$

Let

$$Z(p_k)=\frac{D(p_k)}{M(p_k)}$$

Then

$$Z'(p_k)=\frac{M(p_k)D'(p_k)-D(p_k)M'(p_k)}{[M(p_k)]^2}$$

$$=\frac{D'(p_k)}{M(p_k)}\text{ because } D(p_k)=0$$

if we substitute in equation 1

$$i(t)=i_0(t)+\sum_{K=2}^{K=2n}\frac{M(p_k)e^{p_kt}}{p_kD'(p_k)}$$

which is another common form of the expansion theorem.

The Calculation of Unbalanced Magnetic Pull in Synchronous and Induction Motors

Discussion and author's closure of paper 43-99 by R. C. Robinson, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 620–4.

C. J. Fechheimer (Louis Allis Company, Milwaukee, Wis.): Mr. Robinson has given consideration to the decrease in unbalanced magnetic pull caused by circulating currents in parallel paths of the stator windings. As the vast majority of induction motors have squirrel-cage rotors in which there is nearly the equivalent of an infinite number of phases, consideration should also be given to the decrease in pull occasioned by circulating currents in the squirrel-cage winding. If the rotor is of the slip-ring type with parallel paths, there are circulating currents between such paths, although they are not so helpful as a squirrel cage. A damper winding on the rotor of a synchronous motor helps in the same way as a squirrel cage on an induction motor, although not to the same degree.

I do not believe that a two-circuit stator winding is necessarily in the same class as a single-circuit winding; for example, if turned through 90 degrees from the axis of symmetry.

I would inquire of the author whether $^1/_{32}$-inch displacement is still considered to be the arbitrary value for synchronous machines. Certainly that value is entirely too large to be used as a basis for calculation in induction motors.

R. C. Robinson: Mr. Fechheimer points out that the presence of either parallel circuits in the rotor winding or a two-parallel stator winding may further reduce the unbalanced magnetic pull. While this is true for certain conditions of air-gap deflection, there are other conditions where there will be no reduction in pull. For instance, a machine operating at synchronous speed with a rotating deflection will have no reduction in pull caused by rotor parallels and at a speed close to synchronous will have only slight reduction. A two-parallel stator winding will produce no reduction in pull when the deflection is along the line between parallels.

Since we do not know in advance what type of deflection the machine may have in operation, it is desirable to make the most pessimistic assumptions. Consequently, the machine should be calculated without regard to any reduction in pull produced by the rotor winding or the two-parallel stator winding.

A displacement of $^1/_{32}$ of an inch is the generally accepted standard for synchronous machines. The use of an arbitrary figure such as this merely serves the purpose of giving a basis of comparison between machines and between the unbalanced pull and the shaft spring constant.

Pole-Face Loss in Solid-Rotor Turbine Generators

Discussion and author's closure of paper 43-76, by W. W. Kuyper, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, volume 62, 1943, pages 827–34.

C. J. Fechheimer (Louis Allis Company, Milwaukee, Wis.): Mr. Kuyper has analyzed the pole-face loss of turbine generators arising from the armature-magnetomotive force. Has he made analysis of the loss due to the tufting of the flux from the stator teeth, which occurs at no load and is generally included in the core loss? It would be interesting also for Mr. Kuyper to tell his views on the beneficial effects secured by grooving the rotor surface, as has been the practice of manufacturers of turbine generators.

C. Concordia (General Electric Company, Schenectady, N. Y.): In regard to the value of rotor-iron permeability to be used in the loss formulas developed by Mr. Kuyper, it should be remembered that the flux components with which we are concerned are usually relatively small pulsating components superposed on a large direct component. For this reason it might seem logical to use an incremental permeability in the relations between flux density and magnetizing force.

Laboratory measurements of the incremental permeability of typical rotor steels for small oscillations (of practically zero frequency) superposed on a direct flux have indicated that the incremental permeability is small and relatively constant. Plotted as a function of the magnitude of the direct component, it starts at the value of initial permeability and decreases at a slow and rather uniform rate with increasing average flux density. In nearly all cases investigated the curves ranged from slightly over to slightly under $\mu = 100$, so it would seem that $\mu = 100$ is a good round figure to use in these loss calculations, as well as in some of the transient short-circuit calculations discussed in reference 2 of Mr. Kuyper's paper.

Since the behavior of the incremental permeability is entirely different from that of the garden variety of permeability, one must use caution in the interpretation of test results in which the direct component of rotor flux is absent.

C. B. Fontaine (General Electric Company, West Lynn, Mass.): Mr. Kuyper's paper is of real value to the turbine-generator designer, inasmuch as it offers a rational means for evaluating a major component of the complex generator load loss. These methods have been used to advantage, not only in determining losses for usual generator stator windings, but also in determining the proper location for omitted coils. An idea of the importance of proper location may be gained by observing that a coil grouping of (5-) (5-) for an 11/18 pitch winding results in three times the pole-face loss calculated for a (3-2) (3-2) winding arrangement.

The calculations, particularly those using Figure 3 of Mr. Kuyper's paper, are not complicated. Curves of loss versus pitch, similar to those of Figure 1 of this discussion, may readily be made up to give loss directly for frequently used winding combinations. Figure 1 represents the calculated loss for two-pole 60-cycle three-phase generators with no coils omitted, and with 21 slots per pole, but applies with slight error to similar generators having 18 or more slots per pole.

The calculated pole-face loss is critically dependent on armature-winding pitch whether or not coils are omitted. Figure 1 shows this relation clearly when all coils are in. An attempt has been made to check this calculated relation between loss and winding pitch by studying results of load-loss tests taken previously on about 100 generators ranging in size from 1,250 kva to 12,500 kva. These generators all had magnetic-rotor retaining rings. The losses were measured calorimetrically during short-circuit heat runs and are not reliable individually. However, when the test load loss minus the calculated extra copper loss is plotted against pitch, the curve representing the average of the test points for a given frame size shows a maximum and a minimum near the same values of pitch as the calculated pole-face-loss curve, but the effect of the pitch on the magnitude of the test-loss curve is not so pronounced as the effect on the calculated pole-face loss.

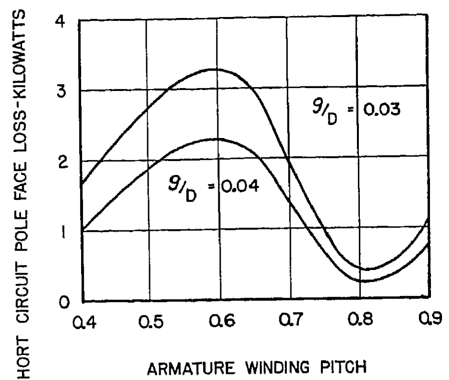


Figure 1. Short-circuit pole-face loss for solid-rotor generators. Calculated for a two-pole 60-cycle three-phase turbine generator, with no coils omitted

$A = 10{,}000$ ampere turns per pole and $L =$ ten inches. Loss is proportional to A^2L

Losses in the stator end structures of turbine generators form an appreciable portion of the total load loss. If the pole-face-loss formulas and the extra copper-loss formulas were correct, the difference between the test load loss and the sum of the two calculated losses would represent mainly the stator end losses. This residue component of the loss was found from the tests above to increase with pitch between about 0.5 pitch and 0.8 pitch, and to dip downward slightly between 0.8 and 0.85 pitch. It is probable that the true end loss contains one component which is relatively independent of winding pitch for a given value of fundamental armature reaction, plus another component which increases with pitch. If this is true, the trend of the test residue loss with pitch appears reasonable, excepting for the downward dip after 0.8 pitch, which may reflect an inaccuracy in the pole-face-loss formulas.

For small two-pole 60-cycle generators we have used the expression, end loss in kilowatts = $(A/10{,}000)^2(D_s/10)$ (K), where A is the square wave value of fundamental armature reaction in ampere turns per pole, and D_s is the stator-bore diameter in inches. K is an arbitrary loss factor, the value of which is determined by test and is plotted against pitch for generators of a given frame diameter and given end construction. Use of the formula and curves have allowed reasonably close predetermination of the short-circuit load loss for those individual generators where the load loss has been measured reliably by dynamometer drive. An average value of K for generators of usual design is roughly 4.

Such a simple expression for end loss, while useful to the designer, does not separate many of the fundamental factors controlling the loss. Consider, for example, a two-pole generator with magnetic-rotor retaining rings operating under short-circuit conditions. The path of the major portion of the fundamental stator-end leakage flux is from the stator end through air to the retaining ring, peripherally around the retaining ring to the opposite pole, through air to the stator end, and peripherally back through the stator end structure to the starting point. The reluctance of the air paths between stator end and retaining ring appear large compared with the remainder of the leakage path through metal, even when the added reluctance due to induced stator end currents is considered. It is probable, therefore, that most of the stator-end leakage flux leaves the stator in the region of the slots, where the air gap to the magnetic retaining ring is relatively short. Since most of this leakage flux links the stator-end windings near the stator core, and before the windings have turned appreciably toward the connection point, it appears that most of the stator-end leakage flux is relatively independent of winding pitch for a given value of armature reaction. This applies as well to the end losses in the region of the stator slots and clamping fingers. The large unlaminated mass of the clamping flange lies above the slot region, however, so that the magnetomotive force, flux density, and losses in this region must be dependent to some extent on pitch. Obviously, the stator end structure configuration and materials, as well as other factors, must also affect appreciably the end losses.

Past papers have presented means for evaluating and controlling extra copper losses. Mr. Kuyper's paper accomplishes the same result for harmonic rotor-surface losses. His work paves the way for a thorough study of losses in the stator-end structure. This additional step must be carried out before evaluation of all major components of load loss may be placed on a rational basis.

W. W. Kuyper: Mr. Fontaine's method of simplifying the calculations is most welcome to designers. It permits a reasonably accurate determination of pole-face loss in a shorter time than is usually required for copper or core losses.

The end loss, as Mr. Fontaine points out, is affected by many factors. A determination of this loss component is difficult, not only because of the geometry of the region, but also because of the wide variety of materials employed. The retaining rings, the stator clamping flanges and fingers, the binding rings, the shape of the stator-coil ends, the type of connections, and the end covers of the machine all affect the loss through their shapes, locations, and the materials of which they are made. It is encouraging that an approximate calculation can be made by a simple formula for a group of machines which is of similar construction.

Because tests give us the sum of pole-face and end losses together with the more accurately determinable extra copper loss, it appears that further progress in the accuracy of predetermining one component is conditioned upon advancement in the quantitative understanding of the other component. The use of accurate testing methods employing dynamometer drive will be a powerful tool in these studies.

The facts which Mr. Concordia presents concerning incremental permeability probably are applicable directly to the pole-face-loss problem and should be considered by those who use the subject paper.

As suggested by Mr. Fechheimer, attempts were made to apply the method to the flux-tufting problem. The calculations showed negligible values of loss for turbine generators of usual design, and the study was abandoned.

That grooving the rotor reduces the pole-face losses has been determined by test. The usual grooving employed is a square thread of about one-fourth-inch pitch. Such a coarse groove is evidently not the ideal, and it is to be expected that calculations for the ideal case of a perfectly laminated layer on the surface of the rotor will show a greater reduction of loss caused by grooving than actually exists.

Such calculations have been made, and the following results are shown to indicate what can be done with perfect grooving:

Let region 1 be the main rotor body
region 2 be the grooved region
region 3 be the air gap
region 4 be the stator

The conductivity of region *2* is assumed to be zero, which, in the ideal case, is true for axial currents. The region is assumed to be homogeneous to the passage of flux. The thickness of region 2 is d.

The loss is given by equation 19 where

$$R=\frac{q_i(1-\tanh^2 ad)(1-\tanh^2 ag)}{\left[\tanh ag+\mu_2 q_r \tanh ad \tanh ag+\frac{1}{\mu_2}\times \tanh ad+q_r\right]^2+[\mu_2 q_i \tanh ad \tanh ag+q_i]^2}$$

This equation for R may be plotted as in Figure 2 of the paper, provided the ratio d/g is fixed and μ_2 is fixed. For $a/g = 0.1$, $\mu_2 = 50$ the $g/l = 0.01$ contour (about the order of the fundamental) shows practically no change; the peak of the 0.05 contour is reduced to two thirds of the value shown in Figure 2; and the peak of the 0.20 contour is reduced to one fifth.

Because tests show that the reduction of loss from the relatively coarse grooving is definitely less than is indicated by calculations for the ideal case, the calculations for the grooved rotor were omitted from the paper. All the test values reported in the paper apply to machines with rotors grooved with square screw threads. Though the benefits of grooving with a coarse thread are appreciable, they do not now appear to warrant any additional complication in the method of calculation.

The Quadrature Synchronous Reactance of Salient-Pole Synchronous Machines

Discussion and authors' closure of paper 43-89 by R. V. Shepherd and C. E. Kilbourne, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 684–9.

C. J. Fechheimer (Louis Allis Company, Milwaukee, Wis.): The maximum-lagging-current test, being dependent upon the reluctance torque, would usually imply that the field member has salient poles. With the round-rotor construction, as used in turbine generators, there are relatively large central poles with slots and teeth adjacent to them. There is still some reluctance torque with this construction, and I would inquire of the authors whether their maximum lagging-current-test method is applicable to turbine-generator construction.

R. V. Shepherd and C. E. Kilbourne: As Mr. Fechheimer points out, the maximum-lagging-current test depends upon the machine being able to develop reluctance torque, but it should also be remembered that the success of this method demands that the load angle at pull-out must be small. It is for this condition that $\sin^2 \delta$ is negligibly small and equation 28 reduces to equation 29 from which the simple relation $X_q=E/I_1$ obtains.

If we limit the pull-out angle to 15 degrees or less, the curves of Figure 8 of the paper show what friction and windage power (P_1) are allowable for a machine with given reactances. For values of X_q/X_d approaching unity the value of P_1 must become vanishingly small to limit the pull-out angle to 15 degrees. Typical values for turbine-generator construction indicate that this condition is not usually met; hence, the method would not generally be satisfactory.

It may be interesting to point out that when an ideal salient-pole machine is operating at no load and zero field current the axis of stator magnetomotive force is in line with the direct axis, and the stator current is, neglecting saturation, e/X_d. As negative excitation is applied, the effect is to decrease the reactance seen by the stator in the direct axis. When the negative excitation is increased to the point where the apparent reactance in the direct axis is decreased to a value equal to X_q, the machine becomes essentially a round-rotor machine as seen by the stator, and becomes unstable. This point of view is modified easily to account for the small load angle necessary to overcome friction and windage losses and gives a good physical picture of the phenomenon. Of course, after the machine has slipped a pole the stator again "sees" the saliency of the rotor, since it is operating with positive excitation in its new position.

Distribution Factors and Pitch Factors of the Harmonics of a Fractional-Slot Winding

Discussion and author's closure of paper 43-100 by M. M. Liwschitz, presented at the AIEE national technical meeting, Cleveland Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 664–6.

C. G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Professor Liwschitz has presented an interesting paper on a subject which seems to intrigue the mathematically minded. Other papers on this same subject have been presented by Malti and Herzog[1] and by Calvert. Incidentally, Professor Liwschitz demonstrates the truth of the more or less well-known but frequently forgotten truth that the distribution factor for the principal harmonic of a fractional-slot winding having N/B slots per pole per phase is the same as the distribution factor of a machine having N slots per pole per phase.

Since Professor Calvert published a rather extensive prior treatise on this subject, I believe it would be helpful to point out here some of the similarities and some of the differences between the two papers which might not be at once obvious to one studying both papers.

One difference is the definition of "fundamental wave." Professor Calvert defines the fundamental wave as the one having a length equal to one full repeatable group, whereas Professor Liwschitz always makes the fundamental a two-pole wave. For example, consider a machine having 20 poles and 96 slots. If the procedure of Professor Liwschitz is followed, the principal flux wave (20 poles) or synchronous harmonic is referred to as the tenth harmonic. The magnetomotive-force waves in this machine are similar to those in a ten-pole 48-slot machine, but, in the latter case, the synchronous harmonic is of the fifth order. Professor Calvert would consider the synchronous harmonic of both machines as of the fifth order; the fundamental wave would have four poles in the 20-pole machine and two poles in the 10-pole machine. Both systems of definition have their own respective merits, but it probably would be helpful if all would define "fundamental" the same way. Professor Liwschitz, however, does assign the number ν to each harmonic, and the value of this number appears to be what Professor Calvert calls "order of the harmonic." My personal preference would be to call ν the "order of the harmonic," for the only harmonics that can exist are those for which ν is a whole number.

Professor Liwschitz finally arrives at the three equations, 15, 16, and 17, for computing the distribution factors of the various harmonics. (Of interest is the fact that these equations are in terms of ν, not n'.) The author helpfully illustrates the use of these formulas by giving numerical examples. Attempts to use these formulas when results were compared with those given by Professor Calvert in his Iowa State College *Bulletin* 142 brought to light some interesting facts:

To compare distribution factors, one must remember that Liwschitz' ν corresponds to Calvert's n. In the author's example of $N=11$, $\beta=4$, Liwschitz' "second harmonic" is Calvert's first, and Liwschitz' "eighth harmonic" is Calvert's fourth.

When β (Calvert's N) is an even number, the Liwschitz distribution factor is twice the Calvert distribution factor. This difference is again a matter of definition, because Calvert uses his distribution factor differently. My personal preference would be for the Liwschitz definition in this case, because his distribution factor is approximately 0.96 for the synchronous harmonic in all cases, whereas Calvert's distribution is approximately 0.48.

Apparently, equations 15, 16, and 17 give the correct magnitudes of distribution factors for most practical cases, where triple harmonics (harmonics of order that ν is a multiple of three) do not occur. However, triple harmonics do have an effect on the zero-sequence reactance of synchronous machines. For such cases the simplified equations yield distribution factors half of what they apparently should be. In other words, if ν is a multiple of 3, the numerator of equations 15, 16, and 17 should be 1.0 instead of 0.5.

Algebraic signs of distribution factors are probably of more academic than practical interest, but it might be observed that the algebraic signs obtained from equations 15, 16, and 17 do not always check those given by Calvert.

The fact that the three final equations given by Professor Liwschitz are not applicable to triple harmonics, unless the numerator is taken as 1.0, and the fact that they do not always yield correct algebraic signs, is not caused by any weakness in Professor Liwschitz' slot-star method, but it is apparently a natural result of the simplifying assumptions which made the numerators of all three equations equal to 0.5.

Professor Liwschitz' equations 20 and 21 for the pitch factors prompt a question. For integral-slot windings there are two common expressions for the pitch factor of a coil having a throw of ρ electrical degrees

$$K_{pn}=\sin\frac{n\rho}{2} \qquad (1)$$

$$K_{pn}=\cos\frac{n(180-\rho)}{2} \qquad (2)$$

The first equation yields correct magnitudes for the pitch factors for all harmonics but not the correct algebraic signs. Equation 2 gives the correct sign and magnitude of the pitch factor for all odd harmonics. Professor Liwschitz' pitch-factor equations have the form of the first equation (particularly, see his equation 18), which is known and acknowledged not to be correct for all algebraic signs. Would not the following form of equation for pitch factor yield both correct sign and magnitude for all odd values of ν?

$$Kpn=\cos\nu\left(90-\frac{\rho}{2\nu_s}\right)$$

where

ν_s = value of ν for synchronous harmonic

ρ = coil throw expressed in electrical degrees of the synchronous harmonic

Reference

1. Fractional-Slot and Dead-Coil Windings, Michel G. Malti, Fritz Herzog. AIEE Transactions, volume 59, 1940, pages 782–94.

M. M. Liwschitz: The use of a single reference wave for all machines in connection with the slot diagram makes it possible to derive simple formulas for the distribution factors of fractional-slot windings by simple reasoning, whereas the use of different reference waves in connection with charts leads to rather extensive mathematical derivations. Through the use of a single reference wave and of the slot diagram, the problem becomes a simple vector problem rather than a complicated mathematical problem.

The remarks made by Mr. Veinott concerning the signs of the distribution factors and the magnitudes of the triple harmonics are based on a misunderstanding. Mr. Veinott assumes that equations 15, 16, and 17 of the paper are the final equations for the distribution factors. The final equations for the distribution factors are the equations 10 and 11. These equations give the correct magnitudes and correct signs for all harmonics. Equations 15, 16, and 17 are simplified equations determined to meet the problems which the engineer usually encounters in practice; in these problems neither the signs of the harmonics nor the triple harmonics are of importance (in order to avoid that the same error be made by other readers, a corresponding remark has been made in the proofs).

With regard to the question by Mr. Veinott of whether it would be advisable to modify formula 2 of his discussion for the fractional-slot windings in order to get the right signs, I would advise not doing this for two reasons:

1. This formula is limited to odd harmonics only and can be applied neither to the fractional-slot windings with even β nor to the chorded 120-degree phase-belt windings.

2. There is no necessity of using this equation (even for the 60-degree phase-belt winding, where it gives correct signs).

The Fourier series, when consistently applied, yields the right signs for all cases. So, for example, for the odd harmonics of the electromotive force it yields the factor

$$k_n=\sin\frac{n\zeta}{2}\sin n\frac{\pi}{2}$$

which gives the same signs as formula 2. The first multiplicand of the factor k_n is the expression given by formula 1 of Mr. Veinott's discussion. It is clear that without the factor, $\sin n(\pi/2)$, this formula can not give the right signs.

It is customary to call the factor which contains the coil pitch "pitch factor." The magnitudes as well as the signs of the pitch factor, as yielded by the Fourier series, are treated in my paper. The signs of the electromotive-force and magnetomotive-force harmonics which depend on different factors are not in the scope of my paper.

Critical-Material Conservation in Induction-Motor Manufacture

Discussion and author's closure of paper 43-82 by H. M. Hobart, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 549–52.

P. M. Lincoln (Therm-Electric Company, Inc., Ithaca, N. Y.): Mr. Hobart points out that the heat-resisting abilities of the usual insulations used in electric equipment—papers, silks, cottons, linens, and so forth—have increased during the last 40 or 50 years by something of the order of 15 to 20 per cent. This estimate agrees with my own experience which began some time in the early 1890's. Mr. Hobart further points out that this increase in the heat-resisting abilities of insulations has resulted in the ability to increase the ratings of motors and other electric equipment by some 30 to 35 per cent. Or, to put the matter in another way, the increase in the heat resistance of insulations that has taken place during the last 40 or 50 years has enabled us to decrease the weight of electric motors and generators by some 30 to 35 per cent. Mr. Hobart's proof of this is entirely satisfactory to me.

But why stop with a 15 or 20 per cent increase in the heat-resisting abilities of our insulations? There are at least three types of insulation that are practically heat proof. These are mica, asbestos, and glass. There may be other types with which I am not familiar. Mica, asbestos, and fiber glass are not particularly good as insulators, but they are heat proof. When I say "heat proof" I mean this in a relative sense. When these heat-resistance materials are used in electric equipment, it is necessary to use a certain amount of tape, varnish, or other materials which are non-heat-resisting. The heat-resisting portions of such a compound resistance material are unaffected by temperatures vastly higher than the maximum temperature to which modern resistance may be subjected, but the non-heat-resisting portions are eventually reduced to carbon by temperatures only slightly above those to which our modern insulations may be subjected. The reduction to carbon, of course, destroys the effectiveness of our insulation. If we could develop insulations which could withstand temperatures 50, 100, 200, 300, 400 per cent, or perhaps even a higher percentage above the best of our modern insulations, think of the tremendous decreases in weight of electric equipment that might be accomplished. Please understand that I am not advocating this increase in the heat-resisting qualities of our insulations but am simply pointing out the possibilities. Any such modification in design must be taken gradually and no forward step made in the heat resistance of our insulations until it has been fully demonstrated that a further increase is safe.

If we attempt increases in the ratings of electric equipment in the manner suggested, there are many other problems that are sure to arise. Mr. Hobart's paper discusses one of these problems. When we decrease the weight of our motors by 30 or 35 per cent by using a better insulation, the problem of starting becomes important. Mr. Hobart proves—at least to my satisfaction—that with a decrease in motor weight of 30 to 35 per cent, the starting problem can easily be taken care of. This may not be the case when the decrease in weight is of the order of 300 or 400 per cent instead of 30 to 35 per cent. In this case, it simply becomes another problem for the electrical engineer to solve. And there are other problems that are sure to arise if we attempt to decrease weights by the amounts suggested. If we attempt to operate electric equipment at 300 or 400 degrees centigrade, each motor becomes a fire hazard. However, it is my belief that if the electrical engineer were confronted with that problem, he could produce a design that would be reasonably safe from a fire-hazard standpoint.

In my opinion a research on the means by which our modern electric equipment may be made safe for temperatures not limited to 115 or 120 degrees centigrade, but which may safely go to 200, 300, 400 degrees centigrade—or perhaps even higher—is one of the problems of research which should be undertaken at once.

H. Weichsel (Wagner Electric Corporation, St. Louis, Mo.): It is very timely to study means of reducing the requirements for critical materials. For induction motors, this is similar to a study of means of increasing the horsepower rating for a given frame size. One author quoted by Mr. Hobart suggests giving the motor a higher normal rating, that is overloading the motor. One of the disadvantages of this is that the starting torque expressed in per cent on the basis of the new full-load rating is decreased. Below it is proved that, contrary to this, the arrangement proposed by Mr. Hobart has the advantage that the starting torque expressed in per cent on the basis of the new full-load rating is increased.

The characteristics of a 15-horsepower four-pole 440-volt three-phase motor connected as proposed by Mr. Hobart will be analyzed. Figure 1 represents its circle diagram. The locus for the primary current when motor is operating without capacitors is the circle 0′–6′–10′. The starting torque is 63 foot-pounds = 145 per cent; the maximum horsepower equals 29.5 horsepower = 197 per cent. The full load corresponds to point 2′.

Parallel capacitors with 7.2 amperes leading current draw shift the full-load point to 2 corresponding to 100 per cent power factor. The locus for the line-current vector is the circle 0–6–10. A capacitor is in series with each line. Its voltage for load point 5 is represented by the vector $f-g$ and is a certain fraction of the vector $z-5$. The required line voltage is $z-g$. With varying load, the point g travels on a circle having a diameter

$d=D\cdot m$

$D=$diameter of main circle

$$m=\frac{\text{length of voltage vector}}{\text{length of current vector}}$$

The top of the circle is a distance $a\cdot m(a=$ distance $0'-z)$ from the end of the motor voltage vector $z-f$.

Increasing the line voltage $z-g$ to the value of $z-f$ increases all vectors in the relation $(f-z)/(f-g)$, but the angle $\alpha=5-z-f$ remains fixed. The new line current in the direction of $z-5$ has a magnitude $z-5\times(f-z)/(f-g)$ (see Figure 2). This process repeated leads to points 1 to 10 (Figure 2) located on a circle.

The locus of the motor current vector is circle 0′ to 10′. The series-capacitor voltage vector has one end fixed at the point 254. Its other end travels on a small circle 1 to 10. Equal numbers on the small and big circle belong to the same load condition.

For the locked condition, the stator copper loss is line 11′–12′ (Figure 2), and the rotor copper loss is the line 10′–12′. The introduction of series capacitors increases the maximum horsepower from 29.5 to 39.5 (Figures 1 and 2). Thus the machine originally rated 15 can now be rated 20 horsepower. The starting torque is increased from 63 to 172 foot-pounds. The line starting current is increased from 86 amperes (475 per cent) to 125 amperes (520 per cent). Thus the addition of capacitors not only raised the maximum horsepower but also improved the starting conditions materially.

The full-load current at the original 15-horsepower rating is 18.1 amperes. When operating at 20 horsepower with series and parallel capacitors, the motor current is 23.6 amperes. Thus the stator copper loss is 1.7 times the loss of the original 15-horsepower machine. Figure 2 shows that for the 20-horsepower load the motor voltage is equal to that of the original machine at 15 horsepower. Consequently, the iron loss in the machine in Figures 1 and 2 is identical. If the machine was originally a 15-horsepower 40-degree machine, it can be expected, when equipped with capacitors, to have at 20 horsepower 65 to 70 degrees rise. This rise is satisfactory if the motor is wound with asbestos wire or its equivalent.

The diagrams discussed assume that each parallel capacitance has a current draw of 7.2 amperes with 254 volts corresponding to 75 microfarads, corresponding to a total of 5.49 kva per machine.

Each series capacitance is calculated to give the motor about 40 maximum horsepower. The line current is 21.9 amperes at 20-horsepower load. The series-capacitor voltage is 33, corresponding to 1,750 microfarads. The series capacitors handle 2.16 kva per machine at 20-horsepower load.

Thus a total rated capacitor capacitance of 7.65 kva per machine is required to increase the motor rating from 15 to 20 horsepower, provided starting conditions are disregarded.

Figure 2 shows that under starting conditions the parallel-capacitor voltage (vector z-10 on the small circle) is $410/254=1.6$ times the voltage rating of the capacitor, a quite satisfactory ratio for commercial capacitors.

The series-capacitor voltage at starting is 570 per cent of normal, because the starting current is 570 per cent of normal. Commercial capacitors are tested for one minute with two times rated voltage. To withstand the starting condition, a capacitor with a normal rating of at least 285 per cent of its full operating voltage is required.

Series capacitors rated for normal operating voltage can be used, provided they are short-circuited or shunted during starting. When current transformers supply the series capacitors, the transformation ratio can be altered during starting when it is desired to obtain decreased series capacitor starting voltage.

It was mentioned that the locus for the line as well as the motor current is a circle. This follows from the equivalent diagram of an induction motor. By adding to this diagram series- as well as parallel-capacitive reactance values, the series and parallel reactances of the diagrams are changed. Only the numerical values of reactances are altered without altering the principle of the diagram. It is known that the locus of current drawn by said equivalent circuit is a circle.

From diagrams Figures 1 and 2, it follows that the voltage conditions on the series capacitors become more favorable when the machine is inherently a poor power-factor machine as a slow-speed induction motor has inherently a lower per cent starting current and starting torque.

Let it be assumed that the no-load saturation curve of the 15-horsepower motor is

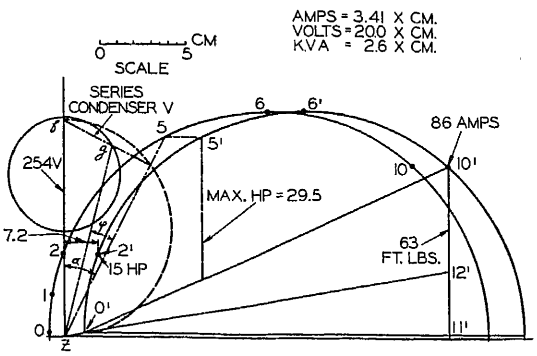


Figure 1 (left). 15-horsepower 60-cycle 440-volt three-phase motor with series and parallel capacitors

Motor voltage held constant

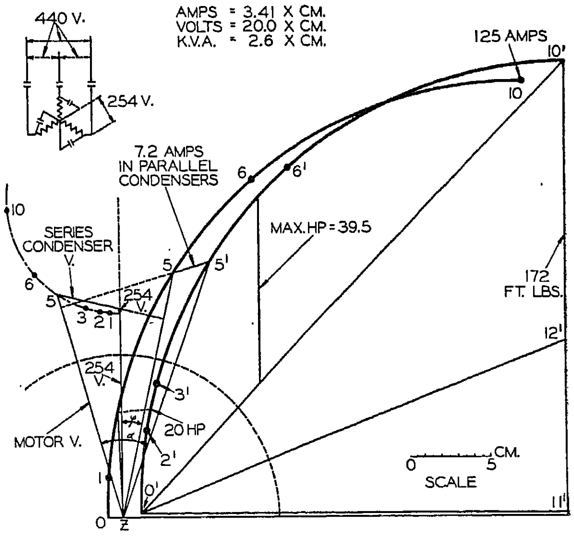


Figure 2 (right). Arrangement same as in Figure 1 but line volts held constant

Full load of motor is now 20 horsepower

given by Figure 3 and that the 15-horsepower machine is rewound with 86.5 per cent of the original turns, corresponding to a maximum horsepower output of 39.5 provided the copper section per slot remains unchanged. The starting current increases from 86 to 115 amperes while the per cent starting torque remains the same. Because of saturation, the power factor at 20 horsepower is materially lower than for the original machine at 15 horsepower. When the original machine has a highly saturated iron circuit, its rewinding for higher horsepower may force the induction to excessive values, making the machine a commercial impossibility. On the other hand, if the original machine is operated with capacitors, the saturation for normal operating conditions does not change materially; consequently an increase of its rating is possible.

If the original machine is a poor power-factor machine (high number of poles), all conditions become more favorable for the machine using series and parallel capacitors, and the conditions become materially poorer or even impossible for the rewound motor. In every case an analysis should be made to determine if rewinding the motor with decreased number of turns or the addition of series and parallel capacitors gives the more desirable results.

The outstanding results are listed in Table I.

Table I

	Original 15-Horsepower Motor	Modified to Give 20-Horsepower Full-Load Output by: Rewinding With 86.5 Per Cent of the Original Number of Turns	Modified to Give 20-Horsepower Full-Load Output by: Using Series and Parallel Capacitors
Full-load amperes	18.1	25.6	21.9
Full-load power factor	92 per cent	88 per cent	99.9 per cent leading
Locked current amperes	86	125	125
Locked current, percent of full load	475 per cent	450 per cent	570 per cent
Locked torque (foot-pounds)	63	86	172
In per cent of synchronous horsepower torque	145 per cent	145 per cent	290 per cent
Maximum horsepower, (full load)	195 per cent	195 per cent	197 per cent
Stator copper loss (watts) full load	434	670	730
Rotor copper losses (watts) full load	640	850	1,140
Constant losses (watts)	508	780	508
Total losses (watts)	1,582	2,300	2,388
Output (watts)	11,200	14,900	14,900
Input (watts)	12,782	17,200	17,278
Efficiency	88 per cent	86.5 per cent	86.4 per cent
Total parallel-capacitor kilovolt-ampere rating			5.49
Total series-capacitor kilovolt-ampere rating			
(a). Capacitor in circuit during starting			6.2
(b). Capacitor cut out during starting			2.16

The numerical values shown in this table hold true only for the particular machine investigated.

Walter L. Upson (Torrington Manufacturing Company, Torrington, Conn.): Mr. Hobart's very interesting paper is aimed at meeting a temporary need. We are inclined to question the substantial gains to be effected in savings of materials and would ask the author to compare savings of copper with correspondingly increased demands of metal for the six capacitors required for each motor.

On the other hand, this paper suggests a peacetime improvement which may be particularly pertinent to postwar progress.

Induction-motor design has changed measurably since 1924, the date of the original proposal mentioned by Mr. Hobart, and, during the intervening period capacitors also have undergone considerable development. The application of condensers to induction motors has resulted in the so-called capacitor-type motor which has made a distinctive place for itself. Nevertheless, in spite of the 20-odd years of capacitor development for motor applications, and the more than 50 years development of the induction motor itself with its resulting high degree of commercial perfection, one may well question whether we have not reached a period favorable to another major advance in induction-motor design and practice.

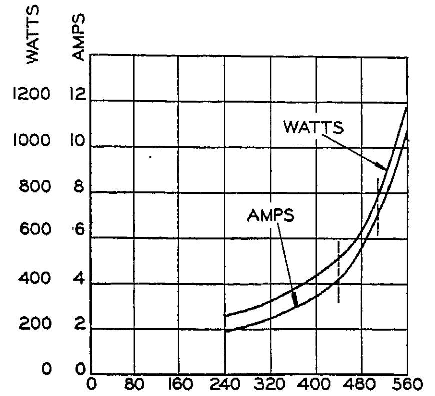


Figure 3. Idle amperes and "constant" losses as function of impressed voltage

Transformer practice, in spite of its apparently even greater perfection, seems to have made greater progress in development than the induction motor during this period.

Mr. Hobart mentions improved performance with the application of capacitors by which terminal voltage is variable with load. This variation has the effect of varying the points of maximum efficiency and power factor, keeping them closer to the load position, which, in itself, benefits performance.

In future developments this relationship of efficiency to load deserves careful study along with such obvious considerations as the use of improved core materials and insulations, better ventilation, and more compact spacing.

Going beyond these is the possibility of applications which may profit by use of a frequency greater than 60 cycles, in which capacitors would become even more commercially practicable, high-power-factor performance more desirable, and weight of copper and steel reduced.

Since there are ways of obtaining higher frequencies from 60-cycle circuits, as well as *de novo*, it would seem highly desirable that the whole subject of induction-motor design and system operation be studied with a measure of detachment from the limitations of standard practice.

One other thought provoked by the paper bears more directly on the subject of wartime saving of materials. It is that greater care might be exercised in suiting a motor to its load. Since standards are meant to cover operations up to a certain point, for example, continuous operation in 104 degrees ambient, if a 75-horsepower motor were required for a given installation; in another installation with the same full load torque, possibly a 50-horsepower motor would be satisfactory if the load were intermittent and the ambient was appreciably lower. In other words, wherever practicable, recommend use of an under- rather than an over-size motor. The same applies particularly to cases where the actual load requirement falls between standard sizes and where the danger of excess load is a minimum.

Comfort A. Adams (Edward G. Budd Manufacturing Company, Philadelphia, Pa.): The author suggests two steps in the attainment of his objective:

1. The employment of high-temperature insulation in order to make safe the proposed increase in rating.
2. The employment of both series and parallel capacitors to increase the stalling torque and to improve the over-all power factor.

The first step has been and is being increasingly employed to the proposed end.

The employment of parallel capacitors for power-factor correction is also a familiar device, the use of which is usually determined by economic considerations, although there are doubtless many cases where they are not now used, but where such considerations would dictate their use, particularly with the present price and quality of the capacitors.

However, the additional use of series capacitors for automatic voltage control, although fully explained in the author's patent of 17 years' standing, has not been employed to any appreciable extent, so far as the writer is aware.

Since there can be no question as to the author's general claims concerning the advantages, assuming the proper choice of the capacitors for both the series and parallel groups, the question naturally arises as to why the series capacitors have not been employed. It is thus unfortunate that the author did not present more complete data as to the capacitance of the series capacitors and as to their cost, rather than stating their kilovolt-amperes for the one example cited. The cost per kilovolt-ampere at the relatively low voltage usually required for series capacitors, would certainly be considerably greater than for those operating at the higher and more normal voltages. This would be particularly true in this case, since it seems likely that the series capacitors would have to be designed to stand transient voltages at least equal to the rated voltage of the motor.

In other words, is it not likely that the kilovolt-ampere rating of the series capacitors would have to be seven or eight times their working load, or practically equal to the kilovolt-ampere rating of the motor itself?

If this reasoning is sound, it may be the explanation of the lack of use of this device.

If, however, the assumptions are not correct, and if the cost of these series capacitors per kilovolt-ampere of the actual load which they carry is no greater than that of capacitors for the normal voltage range, there would be no question as to the desirability of employing this device for raising the stalling torque of induction motors to a point commensurate with the increased rating of the motors themselves, made possible by higher-temperature insulation and high operating temperatures.

S. D. Sprong (consulting engineer, New York, N. Y.): It occurred to me that many would be interested in knowing approximately the prorata cost per horsepower of the additional capacity brought about by your application of series and shunt capacitors. Obviously this horsepower increment would be relatively low as compared with the original installation, and figures covering this might give added emphasis to the commercial side of the problem.

The second thought that occurred to me is whether many of the present commercial motors would have sufficient leeway in the stator laminations to utilize advantageously the increase in applied voltage from 480 to 555 without approaching too closely the knee of saturation curve. Probably your calculations have already taken this factor into account, but I was not able to read into the paper anything covering this point.

Speaking in general terms the overmotoring has been largely due to the purchasers for not predetermining more exactly the power requirements and duty cycle required of the motors. It was probably easier to allow a generous margin of safety for this undetermined factor, contingencies, and so forth, and possibly increase of tool speed as the result of the use of special tool steels, and further the addition of added tools where they were gang-driven. Referring particularly to gang drive there are usually some units not essential to continuous operation and can therefore be dropped off automatically for a time if the coincident load exceeds the safe motor capacity. This has been done successfully in several installations that I happen to know about. It permitted a close measure of motor capacity to essential demand and also kept down the demand charge where this entered into the power-rate contract. It occurs to me that this general idea might still be applied in some installations where motor capacities have about reached the limit.

C. J. Fechheimer (The Louis Allis Company, Milwaukee, Wis.): We have heard recently recommendations for operating electrical machinery at higher temperatures. It is generally assumed that, if the insulation will not fail, it is safe to operate the apparatus at high temperatures. It is also assumed that the higher the temperature the smaller the machine, with a corresponding reduction in weight. It is my desire to call attention to a fallacy in these assumptions, and to add a word of caution.

Entirely aside from the question of the insulation being suitable to withstand high temperatures, we must remember that, as the temperature is increased, the copper resistance and therefore the I^2R is increased, and as the I^2R increases the temperature is increased. Thus, with constant current, the whole is cumulative, and a point of instability may be reached, beyond which the temperature will never cease to rise. As electrical apparatus is usually operated, such instability is not reached. A general figure as to the point of instability can not be given, as it involves the constants and proportions associated with the thermal drop through the insulating wall. If all heat due to I^2R flows transversely through the insulating wall for the part of an armature coil embedded in the iron, then the drop through the wall is given by equation 13 in my paper, "Longitudinal and Transverse Heat Flow in Slot-Wound Armature Coils," in the 1921 AIEE TRANSACTIONS,[1] as follows:

$$\theta=\frac{K\theta_i+\epsilon I^2R}{K-\epsilon I^2R\alpha}=\text{copper temperature above zero degrees centigrade}$$

where

$$K=\frac{\text{transverse heat conductivity of insulation}\times\text{mean coil perimeter}}{\text{insulation thickness}}$$

θ_i = iron temperature above zero degrees centigrade

ϵ = eddy factor in copper conductors

I^2R = loss in copper per coil per unit length axially at zero degrees

α = temperature coefficient of resistance of copper at zero degrees (0.00427)

Evidently, the conditions of instability are secured when the denominator equals zero. Then

$$\text{Critical } I^2R=\frac{K}{\epsilon\alpha}$$

Assume, as a typical case, in an induction-motor slot

Transverse heat conductivity of insulation

$$=0.0025\ \frac{\text{watt}}{\text{inch}\times\text{degrees centigrade}}$$

Mean coil perimeter = 2 inches

Insulation thickness = 0.04 inch

ϵ = eddy factor = 1

Then

$$\text{Critical } I^2R=\frac{0.0025\times 2}{0.04\times 1\times 0.00427}=29.3\text{ watts}$$

The drop through the insulating wall is then

$$\frac{29.3\times 0.04}{2\times 0.0025}=234\text{ degrees for the } I^2R\text{ loss at zero degrees}$$

As the temperature rises, the drop increases so that the drop would be considerably in excess of this value. An experiment on a d-c shunt-field coil some years ago indicated that at around 350 degrees centigrade gases were generated sufficiently rapidly to cause the taping around the coils to be blown off. The temperature was then mounting at a rapid rate.

But the engineer should so proportion his apparatus that the temperatures attained should be considerably below the point of instability. For, as temperatures mount, so do the I^2R losses, and there is little gained by running at high temperatures, from the standpoint of weight and size. Also, in the case of the armature for a motor, the higher temperatures imply reduced efficiency, and therefore more current and greater I^2R.

Would it not be well to reconsider this question, before we allow the pendulum to swing too far in the direction of very high temperatures?

REFERENCE

1. LONGITUDINAL AND TRANSVERSE HEAT FLOW IN SLOT-WOUND ARMATURE COILS, Carl J. Fechheimer. AIEE TRANSACTIONS, volume 40, 1921, pages 589–645.

H. M. Hobart: Professor Lincoln's plea for research on the means by which our modern electrical equipment may be built for satisfactory operation at temperatures much above any to which users are at present accustomed, should be heeded. It is over 30 years ago that Professor Lincoln, Doctor C. A. Adams, and a good many other AIEE members (most of whom are no longer with us in this world), undertook the root and branch revision of this country's electrical-engineering standards. This effort at thorough revision initially was undertaken as an AIEE project, it being felt that a professional body best could guide to success a task of this kind; all sections of the industry being called upon for advice and assistance as occasions arose. We strove to encourage the greater use of mica and other inorganic materials suitable for withstanding high temperatures, and we urged the assignment of considerably increased values for the permitted service temperatures. But the state of the art, and the non-availability, in those days, of suitable heat-withstanding materials for impregnants, adhesives, gums, and varnishes provided the opposition with too many arguments which we could not refute successfully with the result that, even with such stuff as mica, asbestos, lava, and other inorganic substances, the temperature limits to which we could obtain agreement, were decidedly moderate. But the enormous progress which since has resulted from researches on these subjects, now—after 30 years—demands prompt reconsideration. However, instead of attempting the formidable and bewildering task of straightening out the limiting-temperature situation, does it not seem preferable completely and sweepingly to rescind all temperature limitations and simply require of the manufacturers the provision of products with satisfactory life expectancies when employed for their intended purposes? If left to their own responsibilities, manufacturers will proceed to employ materials and constructions of their own choosing, and progress and improvement will be rapid. A manufacturer will employ for each part the material which his experience approves and will so design his product that each part shall withstand in service, for a satisfactory life period, the temperatures to which it will be subjected. In the present standards (which, absurdly enough, show but trifling advances over, or departures from, those established 30 years ago), a fundamental principle of helpful standardization was transgressed in that the manufacturer's freedom was restricted with respect to the means by which he should obtain his object. Fortunately, the will to research resists all discouragements. Each succeeding year, research has put us in possession of more complete mastery of the art of providing insulating materials which successfully withstand subjection to the high temperatures the use of which is often the best means of securing important characteristics desired in various electrical products, such, for example, as lightness, compactness and low cost.

With respect to the kind of insulation employed and the temperature attained, it is reasonable and desirable that the manufacturer shall utilize in his product the advantages accruing from the availability of any superior insulating material or insulating process which he has developed which permits of a higher temperature for a given degree of reliability. To restrict this freedom has the same retarding effect on progress toward better products as would any restrictions to his use of higher permeability castings or lower core-loss laminations or skillfully devised transpositions of conductors. Thus each manufacturer should employ his own best judgment in applying to his product the rating at which he arrives by his special knowledge of its features and characteristics. The manufacturer will supply information as to the continuous or short-time output of his machine at reference conditions and as to the overloads and underloads for which he considers it suitable with service conditions less and more favorable than the reference conditions.

Far less interest, however, should be attached by the user, to the corresponding temperatures of the insulating materials, since responsible manufacturers will not resort to any temperatures not compatible with the properties of the insulations they employ. A manufacturer will be required to produce (with the aid of properly accredited testing laboratories and licensed proving establishments) satisfactory evidence that the materials and constructions which he employs in a given product will not be associated, in the service to which it will be applied, with an annual deterioration of the insulation, greater than corresponds with the agreed life expectancy. The object in view is to limit not the temperature rise but the rate of deterioration of the insulation and other parts. Standardized temperature limitations may have been desirable 30 years ago, at the then state of the art. But it has now led to an undesirable discouragement of initiative, to penalizing the use of the best products, and to placing unjustified confidence in inferior products. In other words, a product intelligently built by an experienced manufacturer employing insulation defined as of a certain class may be more reliable at a temperature rise much above the standardized limit for that class, than another less excellently built product would be when operated at a temperature rise much below the standardized limit.

In his very interesting discussion, Doctor Fechheimer quite rightly emphasized that we should not employ temperatures so great that the rate of increase in the I^2R loss will lead to danger. However, Professor Lincoln was not contemplating any approach to such temperatures in the windings. But by the employment of suitable thermal barriers, we can, in certain designs, advantageously so shield the windings from the laminated magnetic-core material, that the latter's temperature may exceed the winding's temperature by 100 degrees centigrade or more. By such constructions it should be practicable to obtain the advantage of considerable decreases in the hysteresis loss which occur at high temperatures, and to which attention was drawn by the researches carried through by MacLaren in 1911 and 1912.

Doctor Weichsel's contribution to the discussion is so extremely interesting and valuable that it is regrettable that it should be consigned to the rarely read records of Institute discussions. While the author is very proud and encouraged with respect to his own efforts, in realizing that the presentation of his paper should, in part, have been the reason that Doctor Weichsel undertook this fine piece of research in induction-motor design along new lines, it is also his very earnest wish and hope that Doctor Weichsel will make his important findings more promptly accessible to his professional associates and to the entire electrical industry, by embodying the material (suitably and considerably amplified to conform with its importance), in an independent paper of his exclusive authorship, which shall be presented at an early date, on the occasion of an Institute meeting.

Professor Upson puts forward several interesting suggestions, particularly that concerning the increased commercial practicability of capacitor hookups when associated with higher periodicities. It may be interesting to point out that, with the use of higher circuit periodicities, the trend will be toward lower densities in the magnet cores. This bears on a point raised by Mister Sprong who rightly indicates that the approach to saturation in the stator cores imposes a limit to the amount by which (in the proposed hookup), the full-load voltage may be increased above the voltage at light loads. Consequently, with higher periodicities, we may go further with that feature of the project than with the present usual frequency of 60 cycles per second.

The fine quantitative treatment of the problem, for which we are indebted to Doctor Weichsel, provides the kind of information which Doctor Adams justly reproaches the author for not including in his paper. That it was not undertaken by the author was not due to laziness but to his reprehensible ignorance of some aspects of the subject. This applies also to ferroresonance, about which phenomenon the author is likewise completely ignorant. He is informed by a capacitor specialist that, "before the scheme could be exploited," both ferroresonance and the influence of "regions of self-excitation" will require special study. Since this specialist may be right, it is the author's hope that the relation of these phenomena to his induction-motor project, will be given the suggested further study, by someone. Indeed, the specialist was earnestly urged by the author to contribute his knowledge and advice to be embodied in the present discussion. But a letter from him states: "I do not feel that I have anything of sufficient interest to warrant a written discussion for the TRANS-

ACTIONS." It is the author's hope to learn, before long, of some actual trial, somewhere, of this induction-motor project, and of the attainment of good results. It is, however, only reasonable to expect that some troubles will be encountered and overcome, as with all excursions into new fields. A complete exception would be a very agreeable surprise.

In conclusion, let us return for a moment to Professor Lincoln's very timely recommendation to turn our backs on present outmoded temperature practices and examine, in the light of up-to-date knowledge and the industrial needs of the present and the immediate future, plans for a general resort to very much higher temperatures in the service operation of electrical machinery. Attention especially should be drawn to the enormous present and future demand (running literally into many millions of electric motors per year) for motors with ratings of fractions of a horsepower and a few horsepower each, as well as for an unprecedently great number of larger motors. On page 88 of the July 1943 issue of *Electrical Manufacturing*, in a particularly valuable article by Lieutenant Colonel T. B. Holliday, we learn that "in the large airplanes which are to come in the future" the number of electric motors required will approach 100 per plane. To keep down the weight high periodicities of the order of 400 cycles per second, and speeds of many thousands of revolutions per minute usually will be resorted to. All the old conceptions of permissible temperatures will be inapplicable, and all insulations will be inorganic or the equivalent as regards their ability to withstand far higher temperatures than heretofore have been deemed practicable. These aircraft motors will receive their current from generators of extremely light weight, often through interposed lightweight high-temperature transformers.

It is perfectly evident that the consequent shake-up to established conceptions and accepted standards will be utter. The result of demonstrating the possibilities of radical improvement in electric equipment for aircraft inevitably will be that comparable high periodicities and speeds and temperatures promptly will become usual in other fields, instead of being exceptional. This will apply not only to motive power for embodiment in machine tools in workshops, but also in the motors and transformers which will be required by the millions in domestic appliances of all kinds, and in agriculture. In other words, the electrical age definitely has arrived on a great big scale, and much of the equipment, which, heretofore, has been regarded as reasonably adequate, now must be promptly superseded by equipment embodying up-to-date conceptions and constructions, and in which notable advances will be employed as regards high periodicities, much higher service temperatures, very high speeds, lighter weights, and lower costs. These changes will be effected without involving any sacrifices in the efficiencies or life expectancies.

A New Type of Adjustable-Speed Drive for A-C Systems—II

Discussion and authors' closure of paper 43-83 by A. G. Conrad, S. T. Smith, and P. F. Ordung, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 522–5.

C. G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): The authors have recognized the practical difficulty of operating a shunt motor on alternating-current because of the approximate quadrature displacement of the armature current from the field flux. They solve this difficulty by the use of a polyphase supply circuit. This paper reminds the writer of his own intensive efforts to solve this problem 13 years ago using only a single-phase source of power. I attempted to get around the phase-displacement difficulty by using a capacitor in series with the field, making a series-resonant circuit in which the field current would be in phase with the impressed voltage. This arrangement did not work very well, because the short-circuit currents in the coils undergoing commutation, being inductively coupled with the main field circuit, interfered with the resonant circuit and gave rather erratic results. It is probable that the use of a polyphase power source would eliminate this difficulty.

C. W. Drake (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): I have been very much interested in both parts I and II of these papers, but more so in part I presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943 in which the diagram of connections and speed-torque curves was given. Adjustable-speed characteristics as shown, together with regenerative torques, are required by a large number of industrial applications, and such characteristics on a-c motors have been sought ever since the induction motor was first developed.

From year to year, various designs of a-c commutator motors have appeared in this country, but so far no one type has obtained what we would call general acceptance by the trade. This may be due in part to the relative price, performance, or electrical complications of the motor and control equipment; but, since many motors of similar design are used in Europe, we think that the lack of acceptance here is attributable chiefly to the difference in the temperament of the American and European electricians or maintenance men. There is no question about the satisfactory operation of certain types of these motors abroad and in some plants in this country, but, in general, maintenance men here are not trained to, or in most cases are not agreeable to, the frequent and careful inspection required by this class of apparatus in order to insure satisfactory operation. The tendency here seems to be toward equipment which will require attention only at very infrequent intervals, and if the design described in this paper will measure up to these requirements it will undoubtedly overcome many of the limitations of previous types.

In Mr. Veinott's discussion he mentions work he did along these lines 13 years ago, but we also find that B. G. Lamme in 1911 disclosed before the AIEE a design quite similar to the one described by the present authors and mentioned that two motors of 20 and 30 horsepower were built.[1] The 20-horsepower motor was installed in an industrial plant and operated satisfactorily for 18 months, after which it was taken out because of a change in manufacturing conditions. These particular motors operated on a 25-cycle system, so that commutation was much less of a problem than it is now for 60-cycle service.

REFERENCE

1. Discussion by B. G. Lamme of METHODS OF VARYING THE SPEED OF ALTERNATING-CURRENT MOTORS. AIEE TRANSACTIONS, volume 30, 1911, pages 2487–90.

A. G. Conrad, S. T. Smith, and **P. F. Ordung:** Mr. Veinott's explanation of the shunt-a-c motor, with its field excited in series with a capacitor to establish resonance, checks precisely results obtained by other investigators using such circuits. He mentions that the effect of the change in currents in the short-circuited coils with different values of load interferes with the desired resonant condition. Another factor that causes such inteference is the shift of the neutral axis of commutation with change in load. These two factors cause a phase shift of the field flux with respect to the armature voltage as load is changed. This results in large changes in power factor and poor commutation. With the polyphase supply described in the paper, these factors while present do not change the magnitude or direction of the field flux, and, consequently, better commutation is obtained.

Mr. Drake's discussion of reluctance on the part of American engineers and maintenance men to accept commutator types of a-c motors is accurate. Judging from the number and the nature of the inquiries received on the subject, the authors assume that the commutator-type motor is still quite a mystery to many engineers. It is not accepted because it is not understood. The reference to Mr. Lamme's work in 1911 has been covered in the closing discussion of part I of this series of papers.

The Cause and Control of Some Types of Switching Surges

Discussion and author's closure of paper 43-102 by T. W. Schroeder, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 696–700.

R. A. Hentz (Philadelphia Electric Company, Philadelphia, Pa.): It may be of interest to outline the part of the Philadelphia Electric Company system which is similar to the system outlined in the author's paper and to describe methods of operation followed in order to avoid high switching surges.

Two generating stations, *A* and *C*, are tied together by means of 66-kv lines. One of these lines recently installed (see Figure 1 of this discussion) passes through substation *B* and forms one of the two 66-kv "backbones" which extend practically throughout the system. It is separated from the other 66-kv "backbone," though the two are paralleled through reactors on 13.2-kv busses at various points. The new line consists of underground cable from station *A* to station *B* and aerial open wire from station *B* to station *C*.

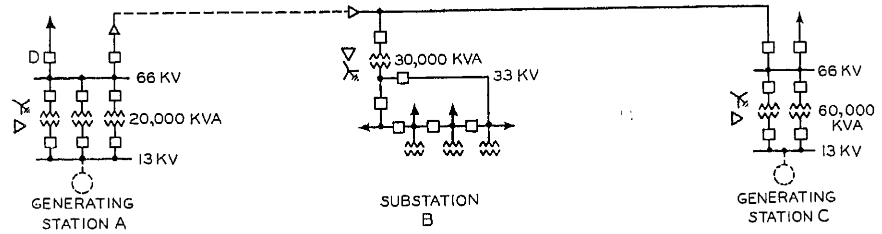


Figure 1

The addition of this underground-cable section, which has a charging kilovolt-amperes of about 20,000, focused attention on methods of switching to insure minimum of switching surge. The problem was considered:

1. As to what would be done in the case of automatic switching for a failure on the 66-kv aerial line or cable constituting this tie.
2. What would be done in the case of manual switching where the time selected for this could be chosen.

In the case of automatic operation, 66-kv circuit breakers at stations *A* and *C* and the 33-kv circuit breakers on the low-voltage side of the transformer bank at station *B* are tripped by pilot-wire relay scheme, thus keeping the station *B* transformer bank, which is delta-connected on the 66-kv side, in circuit to absorb the charge of the cable in the manner indicated in Figure 3A of the author's paper. The only disadvantage of such a procedure is that it is slightly preferable to trip the 66-kv breaker on the station-*B* transformer bank, since the 33-kv installation is a ring bus, and tripping the two 33-kv breakers opens this ring. If this is the only tripping, this in itself is of no great importance, as all of the remaining lines and transformer banks remain connected together and energized from the 33-kv system; however, it would be of importance were there an operation on some other part of this 33-kv installation, as the ring bus would then be opened at a second point, thus creating two 33-kv parts. It is planned, however, that as soon as these 33-kv breakers on the transformer bank trip, the operator will open the 66-kv breaker and a disconnecting switch in the 33-kv leads from the transformer bank and then again close the 33-kv ring by reclosure of these breakers.

Where the switching may be done at the load dispatcher's convenience, the cable is de-energized by first opening the 66-kv circuit breakers at stations *B* and *C*, and circuit breaker *D* at station *A*. The low-voltage (13.2-kv) breakers on the three transformer banks at station *A* are successively opened, the supply to the 66-kv cable being interrupted with the opening of the third low-voltage breaker, leaving the three 20,000-kva transformer banks, which are wye-connected and neutral, solidly grounded on the 66-kv side, to absorb the surge as indicated in Figure 3B of the author's paper. The characteristics of the transformer banks at station *A* and station *B* and of the 66-kv underground cable between these two points are:

Station *A* transformers X_m = 11,300 ohms
Station *B* transformer X_m = 9,920 ohms
Cable X_c = 246 ohms

It will be noted that with the preceding characteristics the ratio of X_c/X_m is about 0.025, which is an unfavorable location for it to be, as indicated in Figure 5 of the author's paper. As an added precaution, line-type lightning arresters were installed at each end of the cable.

It is to be noted that all of the circuit breakers involved in either the automatic or manual operation of the 66-kv cable are of the oil type, though an air-blast breaker is to be installed to replace the oil breaker on the 66-kv side of the station-*B* transformer bank, which, as previously shown, does not enter into any operation while the cable is energized. Some thought was given to the effect of the air-blast breakers; but, if there was any advantage, which was doubtful, it did not seem enough to justify the expense of their installation. In fact, it may be that the rapid extinction of the arc, that is characteristic of the air-blast breaker, may create higher surges than those experienced in oil-circuit breakers where the extinction of the arc is slower.

Since this 66-kv line has gone in service, there have been two automatic operations. One of these was due to a fault on the aerial wire section between stations *B* and *C;* the second was caused by an operating error whereby the live cable was accidentally grounded at substation *B* by closing an air-break switch connecting the cable to the aerial wire section which had been blocked out for work and purposely grounded by tying the three phases together and to ground. Obviously, this ground connection offered a drain for the static charge of cable. Both of these faults were cleared satisfactorily, and there was no apparent disturbance.

E. L. Michelson (Commonwealth Edison Company, Chicago, Ill.): This paper is a valuable addition to the literature on switching transients. These papers take the mystery out of switching transients and reduce their analysis to a well-defined basis.

In Chicago our experience has shown the value of switching transformers with lines. A few severe cases of overvoltage have occurred when 66-kv underground cables were de-energized by opening the line breaker. The most severe cases occurred when the system supplying the line was ungrounded. As a result of this experience, present practice requires that whenever possible these lines should be de-energized by connecting the line in series with a transformer and opening the low-voltage switch on the transformer.

In connection with Figure 3A of Mr. Schroeder's paper, I wonder whether his studies on this setup would enable him to express an opinion on the following problem: In Chicago we have a few situations where a 12-kv three-conductor cable, approximately two miles in length, supplies directly a transformer bank of 7,500-kva in the manner shown by Figure 3A. A single-phase-to-ground fault on the cable will result in the line breaker being opened at the supply end of the system, but there is no provision for immediately disconnecting the transformer from the low-voltage bus for such a fault. This situation leads to a theoretical possibility of an arcing ground with resultant overvoltages on the cable and transformer, and I wonder whether Mr. Schroeder's studies would shed any light on the possibility of such overvoltages being obtained.

L. R. Gaty (Philadelphia Electric Company, Philadelphia, Pa.): The author has prepared an excellent paper giving us another tool to determine the magnitude of switching transients. These become particularly important in switching having high-capacitance kilovolt-amperes. The information in this paper was very helpful in studying the transient voltages which would result from switching a 66-kv cable, installed by Philadelphia Electric Company in 1942, having a capacitance charging kilovolt-amperes of 20,000. A delta-connected transformer bank was connected to one end of this cable and was found to be helpful in minimizing the possibility of high transients. Fortunately, switching could be done on either the high-voltage or the low-voltage side of this bank, and it is now arranged for low-voltage switching. Referring to Figure 5 it is interesting that in this case the ratio of X_c to X_m is about 0.025 giving close to the worst condition.

T. W. Schroeder: I wish to thank Michelson, Hentz, and Gaty for their discussions covering the practical application of some of the thoughts advanced in my paper.

The Philadelphia Electric Company's problem of switching some 20,000 kva of cable capacitance furthered the desirability of more definitely determining the effect of transformer windings on the capacitance-switching problem. Mr. Hentz indicates that the X_c/X_m ratio of 0.025, applying to this system, is in an unfavorable region, referring to Figure 5. While this is true from the standpoint of the ultimate maximum volts which would obtain across the switch, which would be about 2.5 times normal (against twice normal with no restriking in the absence of a transformer), it should be noted that the time required for the switch voltage to reach twice normal is about 1.3 cycles as against 0.5 cycle in the absence of a transformer. This latter is probably the more important consideration for an interrupter capable of interrupting the three phases reasonably simultaneously.

Mr. Michelson's experience, that "high-side" switching in an ungrounded circuit can result in more severe switching overvoltages than in a solidly grounded one, agrees with analyses and other experience.

In connection with the question raised by Mr. Michelson, the circuit he describes is probably not uncommon where transmission is largely at generator voltage. It is now generally believed that an arcing ground in a dielectric of free air is not capable of building up very high overvoltages, because the dielectric strength of the medium in the vicinity of the arc is decreasing rather than building up with time.

Interim Report on Application and Operation of Automatic Reclosing Equipment on Stub Feeders

Discussion of paper 43-84 by the subcommittee on a-c automatic reclosing equipment of the AIEE committee on automatic stations, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 536–40.

G. S. Lunge (General Electric Company, Schenectady, N. Y.): It is appreciated that to keep the length of the questionnaire reasonably short, it was not considered feasible to ask for various additional useful items of information concerning these circuits.

Perhaps one of the most important of such missing items of data is what percentage of the poles were equipped with protective gaps or arresters, and of what type? If these gaps were plain rod gaps, as distinct from expulsion-protector tubes, each time such a gap arcs over from lightning overvoltage, a circuit breaker, oil circuit recloser, or fuse operation will result, after which a successful initial reclosure should always be possible.

It will be obvious that these circuits will necessarily show a higher percentage of successful reclosures than they would if not equipped with plain gaps. Therefore, if it had been known which circuits had such protective gaps, it would have been logical to tabulate the data from such circuits separately, as the number of outages for such circuits will naturally be larger than, and bear no definite relationship to, the number of true line faults.

It would be interesting to know whether enough of the circuit mileage, for which data were received, had protective gaps (other than expulsion protector tubes) to have contributed materially to raising the percentages of successful reclosures to the values reported in Figures 2 and 3 of the report.

While advocating that consideration be given to wider application of immediate reclosing to motor circuits, and pointing out that to avoid losing the induction-motor load, reclosure must be completed within 0.5 second to 2 seconds, it will be noted that the report does not state whether immediate reclosure of motor circuits, without any material time delay, is advisable. Although none of the proposed American Standard *C*37.7 circuit-breaker operating duties for reclosing service mention intentional time delays less than 15 seconds, reclosing relays are available that will permit total de-energization times within the 0.5- to 2-second range advocated. Nevertheless, in the interest of permitting the use of still simpler reclosing relays, it would be of interest to know whether the use of reclosing relays with no intentional time delay, that is, immediate reclosing, has met with widespread success in the case of circuits carrying an appreciable percentage of motor load.

J. A. Elzi (The Commonwealth and Southern Corporation, Jackson, Mich.): As Mr. Lunge has stated, information regarding the type of surge protection provided was not requested in the questionnaire, and data are, therefore, not available to show to what extent the use of open rod gaps would affect the reclosing performance. Moore and Watkins in their paper, "Experience With Oil Circuit Reclosers on REA Systems," did present such data, and the results of their survey would seem to indicate very little difference in the percentage of lockouts for the two types of protection.

Mr. Lunge has pointed out that a Rural Electrification Administration system, though nation-wide in scope, would have transformers on a small percentage of the poles as compared with other distribution circuits. This could very well account for the fact that Moore and Watkins found very little difference in the performance of the two types of surge protection. A review of the REA data as given in Tables IV and VII shows that trees are the greatest single cause of lockouts on REA systems. This would further account for failure to show any decided difference caused by the protective equipment. As Mr. Lunge has pointed out, it would seem reasonable to expect that on a distribution circuit on which plain rod gaps are used for surge protection a higher percentage of successful initial reclosure than the average values given in Figures 2 and 3 could be expected. The number of trip-outs per year probably also would be higher on these circuits than on circuits having transformers protected by lightning arresters or expulsion gaps.

With reference to the question as to whether or not immediate reclosure of motor circuits without material time delay is advisable, it might be stated that the 0.5-second interval given by some of the users was based on the length of time that a circuit would be de-energized using immediate reclosure, the time interval being simply the time required for a normal-speed breaker to open and close. Some users very definitely apply immediate initial-reclosing equipment on circuits carrying an appreciable percentage of motor load, and this practice has been very successful where used.

REFERENCE

1. EXPERIENCE WITH OIL CIRCUIT RECLOSERS ON REA SYSTEMS, Lee M. Moore, Bruce O. Watkins. AIEE TRANSACTIONS, volume 62, 1943, August section, pages 531–5.

Experience With Oil Circuit Reclosers on REA Systems

Discussion and author's closure of paper 43-86 by Lee M. Moore and Bruce O. Watkins, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 531–5.

Edward Beck (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Moore and Mr. Watkins have gathered a large number of data on the operation of their rural systems. Undoubtedly, there are a considerable number of background data available which might be used to present an accurate and detailed discussion of the relative performances of simple gaps and lightning arresters in relation to service continuity. Although the paper contains some data on the subject, we do not believe it is sufficiently complete or detailed to draw conclusions one way or the other. Since there has been some controversy about this subject, a report from the same source as this paper, covering so many miles of line in different parts of the country, would be of considerable interest.

Tables II, III, and VI of the paper indicate that there are less breaker operations per mile of line with arrester-protected transformers than with gap-protected transformers. We understand that the outage following the initial breaker operation is several seconds before the breaker recloses, and that on this type of system such a long outage is not objectionable. It is thus possible that ample service reliability is produced by gaps, particularly since, according to Tables IV and VII, the lockouts that the transformer protection can do something about (items 4 and 5) are comparatively few. Where the outages produced at the transformers are a large factor in the causes of lockouts or where temporary interruptions of several seconds are not permissible, the character of the transformer protection will be of considerable importance. Satisfactory system operation apparently will be obtained on arrester-equipped systems with less breakers per mile than for gaps.

It is apparent that arresters or protector tubes reduce the breaker operations and that, therefore, more reliable service can be given with self-clearing protection, such as arresters or protector tubes. Probably one of the factors that leads some operators to lean toward the simple open gaps is the fact that arresters or protector tubes of suitable quality are more expensive than gaps. On rural circuits, the requirements for successful arrester operation are somewhat modified from those on high-capacity urban systems. The lightning duty is severe, and therefore the 65,000-ampere discharge capacity requirement considered standard should be maintained. However, the fault currents on rural systems are limited; therefore, the current-clearing ability of a protector tube may be less. Advantage can be taken of this in the design. A maximum current rating of 500 amperes should be ample for the majority of applications. Such a device can be made for lower costs than the standard tube with unlimited current rating. The company with which the discusser is associated has manufactured such protectors for use on 7,200-volt circuits. They are identified as the type *B* deion protectors. Some are in service. I would like to ask the authors of this paper whether it is likely that future service requirements will demand greater continuity of service, and whether the economics of such requirements would be satisfied by a combination of reclosing breaker and lightning protectors costing perhaps 75 per cent of those now standard?

H. L. Rawlins (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper marks a real contribution to our knowledge of the distribution field, and the authors are to be thanked for obtaining and presenting the information in such a complete and yet concise form. It covers the operating experience over a wide area, and such a comprehensive survey of reclosing breakers and rural systems would have been difficult to obtain from any other source.

To those who are interested in the design of rural reclosing breakers, the story is complete. The average operating duty on the breakers is revealed, and the detailed analysis of breaker troubles certainly would be of value to the designer. No exceptions can be taken to the proposed specifications covering an ideal small distribution circuit breaker, for the requirements seem both desirable and feasible.

There is a wealth of information in the tables which, upon close inspection, reveal many interesting and puzzling features. All but one of the tables concerning system performance show comparisons between systems with gap-protected transformers and those with arrester-protected transformers. Was this to compare the performance of the different systems, or was it done merely because different operating duties were imposed on the breakers? It is noted that no comparison is drawn favoring one type of system over the other. In classifying the systems, were tube-protected or CSP (completely self-protected) transformers included with the gap-protected transformers or with the arrester-protected transformers?

A study of Table II reveals a marked decrease in the number of breaker operations per mile for arrester-protected systems during September and October; yet there is only a very slight decrease in the number of operations per mile for gap-protected-transformer systems. There were over three times as many breaker operations on the systems with gap-protected transformers. To what do the authors attribute the large number of gap breakdowns during these comparatively lightning-free months?

D. D. Clarke (Kansas City Power and Light Company, Kansas City, Mo.): The authors have presented a splendid résumé of experience in the maintenance of service to an extensive territory of operation. In some 20 years of experience with oil circuit reclosers of various types, it was found that as the art progresses the customer more frequently is not inconvenienced by a successful reclosure. There is evidently some short time of opened circuit for each customer's usage that may be considered as no outage for that customer. In line with this, it would be of great interest to know whether the study of the system experience described by the authors has indicated that the immediate oil circuit recloser has caused the customer inconvenience or noticeable outage, where such reclosure is successfully accomplished.

B. W. Wyman (General Electric Company, Philadelphia, Pa.): Mr. Moore and Mr. Watkins have presented a wealth of operating data which should prove of inestimable value to system planning engineers the country over. This is the first time that actual operating experience with oil circuit reclosers has been made available on a scale large enough to overbalance any influencing factors such as peculiarity of terrain, weather conditions, tree, or bird difficulties.

Although the authors have pointed out that the improvement in service continuity obtained by these reclosing breakers is of far greater importance than the mere operating savings, it is interesting to note that, on over one half of the systems reporting, the maintenance and operating savings alone amount to six dollars per month per breaker. This means that only about 16 months are required to write off the investment costs per recloser.

Inasmuch as the recloser of design 2 has shown such an outstanding performance record, and also because the percentage difficulties have been reduced to such a low value as compared with the older design 1, it may be of interest to note the design features of design 2 which are responsible for this marked improvement.

To increase greatly the impulse insulation strength of the recloser and still to maintain moderate dimensions, the steel-tank construction was eliminated entirely and replaced by a porcelain cylinder of very high insulation strength with the two terminals at opposite ends of the device. Figure 1 of the paper shows this device mounted on a crossarm.

To prevent damage to the series coil from lightning impulses of steep wave front, a Thyrite disk is used to shunt the coil. This disk is small and mounts directly at the coil inside the recloser. This Thyrite disk enables the series trip coil to withstand impulse voltages far in excess of those which would flash over the outside of the porcelain housing. This construction makes possible a device with an impulse insulation strength many times higher than the original steel-tank design 1. The considerable improvement in operating performance obtained is evidenced by the small total percentage of difficulties reported since the second design has been introduced.

The Rural Electrification Administration is to be congratulated on its farsightedness in installing these devices in such large numbers and on the accuracy of their application, as any device owes its success or failure to the way in which it is applied.

F. E. Andrews (Public Service Company of Northern Illinois, Chicago): There are included in the paper suggestions for a specification for a small distribution circuit breaker. These suggestions do not cover the question of time–current characteristics for fault clearing.

On some systems it has been found impractical to apply oil circuit reclosers because of the need for co-ordination with branch-circuit fuses beyond the recloser. If reclosers are applied to a portion of the system sufficiently large in extent to load the recloser to a value approaching its rating, the amount of line beyond the recloser usually will be sufficient to require branch sectionalizing fuses. It has been found impossible to co-ordinate these branch fuses with the circuit reclosers now available, and for that reason they have not been used in many situations for which such devices generally seem needed. Therefore, it would seem that an item to cover circuit-opening time–current characteristics suitable for co-ordination with branch-circuit fuses beyond the recloser should be added to those items listed in the paper as desirable requirements for a small distribution circuit breaker.

Carl E. Mosley (W. N. Matthews Corporation, St. Louis, Mo.): Moore and Watkins are to be commended highly on the fine paper that they have prepared on their "Experience With Oil Circuit Reclosers on REA Systems."

The comments that I have are on parts (*c*) and (*d*) of the possible specification for a small distribution circuit breaker which respectively suggest that the breaker "be entirely automatic," and that the breaker "be equipped with two instantaneous openings and two subsequent time-delay openings to lockout. . . ."

Unquestionably, a breaker with an entirely automatic feature should reduce the number of service outages to a minimum and will require less attention from the operator. However, it is possible to get into trouble if breakers are too automatic and are not periodically inspected. A circuit breaker is like a transformer, with respect to the load current that it is designed to carry. A breaker should be replaced when the load current exceeds its safe current-carrying capacity; otherwise, the breaker will operate unnecessarily without lockout, while a higher-rated breaker will continue in service under the same load conditions. Breakers are likely to remain in service for months, subject to these overload conditions, without the operator knowing it.

If the breaker is oil-filled, these unnecessary operations will carbonize and lower the dielectric strength of the oil which would cause the breaker eventually to fail.

If accurate records of increasing loads are kept by the REA co-operatives, such overload conditions probably will not occur. It seems to me that if a breaker will operate a predetermined number of times and then lockout, either it will give the operator a clue that there is something wrong that should be corrected, or it will remind him to inspect the breaker.

I am of the opinion that to include two instantaneous openings and two subsequent time-delay openings in a circuit breaker would complicate the mechanism and increase the cost of the breaker. This would defeat the requirement (*h*) of the possible specification which states that the breaker should "be inexpensive."

Hamilton Treadway (Rural Electrification Administration, St. Louis, Mo.): The extensive construction of rural electric-distribution systems, which began in the middle of the last decade, raised many new problems for the distribution engineer. Not the least of these was the lack of suitable protective and sectionalizing equipment. The adoption of 7.2-kv multigrounded neutral systems made possible the use of single-pole oil circuit reclosers as one approach to the solution of this problem. The authors' paper discusses the operating experience of REA systems using this type of protective equipment. This experience has been exceptionally good.

The authors have indicated the direction which further development of this method of protection dictates. The advantages of combination instantaneous and time-delay openings have been apparent for a long time. It is obvious that continuity of service will be improved for consumers along the main feeders. Whether sectionalizing in this manner will improve service to all consumers is a problem which must be determined on each individual system. If time-delay opening is to be provided in these breakers, it is desirable that it be made adjustable to provide great flexibility in co-ordination.

The development of a single-pole breaker with time-delay opening will require more accurate determination of fault currents and a more thorough study of co-ordination problems. Also, it will require the manufacturers of fuses to develop characteristics which will bring the time–current curves of the fuses and breakers closer together. While much has been done in this direction already, there is need for further improvement.

One interesting feature of the data presented is the relation of lockouts to the number of operations. Since lockouts are equivalent to permanent faults, the data indicate the percentage of permanent faults to be much lower than was thought to be the case a few years ago. This low percentage of permanent faults is explained partly by the time–current characteristics of these reclosers which increase the number of operations over those experienced with fuse sectionalizing. However, the earlier data may require some revision. This is a point demanding much study, because continuity of service is of first importance in the rural area. It would be interesting to determine what portion of this reduction in permanent faults is due to tree trimming, improved line construction, and so forth. It would prove interesting to break these data down according to the recognized thunderstorm-frequency areas throughout the country to determine the effect of lightning on this factor.

Much could be gained if more factors could be analyzed in the economic data presented. The assembling of future data should be done in a manner to permit an evaluation of the effect of the miles of line and number of consumers per breaker, the distance which must be traveled to restore service to the area controlled by each breaker, number and length of outages per breaker, ratio of permanent to temporary faults, type of roads, weather conditions prevailing in the area, type of transformer protection, and many other factors. It is realized that considerable difficulty is experienced in assembling these data in a form whereby they can be analyzed readily. This, however, is essential to permit the distribution engineer to select sectionalizing points with greater certainty as to over-all economy of operation.

A further comparison between the relative economy of line sectionalizing and branch sectionalizing would be desirable. This again requires data not now available.

Another approach to the problem of protecting rural electric-distribution systems has been made in the area of the Tennessee Valley Authority. Here station-type breakers have been used with a single high-speed opening and reclosure, followed by a time-delay opening. This method has been employed in conjunction with branch fusing. There are many advantages to this system of fault protection, and there are many limitations. It would be interesting to compare the economy of operation of this method of fault protection with the single-pole method described by the authors.

As pointed out by the authors there are many problems yet to be solved in this field. It is hoped that the authors will continue their search for basic data, particularly in the direction of the economics of breaker design and operation.

W. R. Bullard (Ebasco Services, Inc., New York, N. Y.): This paper provides a valuable contribution to the available data concerning the performance of rural distribution circuits equipped with reclosing breakers. Of especial value is the comparison of performance of circuits with gap-protected transformers and that of circuits with arrester-protected transformers. Of course, the results of such a comparison must be used with caution, since there can be no assurance that factors other than the method of lightning protection, which might influence the performance, were sufficiently similar for the two groups of circuits to make the comparison valid. Nevertheless, the large amount of circuit mileage represented and the fact that the systems represented by both groups apparently were distributed among a number of different states encourage the use of the results for drawing tentative conclusions.

An outstanding point of interest is the very small difference between numbers of operations per month per mile for the two groups of circuits. But a more important point is that there is practically no difference between the numbers of lockouts per month per mile for the two groups. In Table III of the paper the comparison is 0.0062 lockout per month per mile for the gap-protected circuits and 0.00598 for the arrester group. In Table VI it is 0.00548 for gaps and 0.00562 for arresters.

It is the lockouts that determine the quality of service, not the momentary interruptions occasioned by opening operations followed by immediate successful reclosure. Therefore, insofar as this particular comparison is indicative of relative performance of arresters and gaps, it is difficult to see how the use of arresters, with their higher first cost and added maintenance problems, can be justified on circuits equipped with reclosing breakers. Furthermore, referring to the data on miles of line per breaker in the various tables which show appreciably longer individual circuits or sections of circuits for the arrester-protected systems, and also the data on lockouts per breaker which show appreciably lower numbers of lockouts per breaker for gap-protected systems, it seems that, as presently constituted, the gap-protected circuits are giving considerably better service than the other group.

If service equivalent to that provided by the arrester-protected group is satisfactory, service quality cannot be considered as justification for the larger number of breakers for a given amount of mileage actually used in the gap-protected group. In other words, the design can be the same for the two groups insofar as length of individual circuits, or sections of circuits, is concerned. Of course, there may have been some other reason for the difference in average mileage per breaker shown in the tables.

Near the end of the paper mention is made of the desirability of a particular sequence of operations, namely, fast opening on the first one or two openings and moderate time delay on succeeding openings. This is a sequence that has been in use for many years and has proved very successful, not only in improving quality of service, but also in eliminating the need for large numbers of reclosers, such as those of which the performance is covered in the paper. Briefly described, the practice concerned consists of controlling a large amount of circuit mileage by a single reclosing breaker located at the substation. This large circuit is sectionalized by simple single-shot fuses. The first opening of the breaker is instantaneous, and, if there is no persistent fault, service is immediately restored to the entire circuit by reclosure. If the fault persists, a time delay is introduced in the second or third opening, causing a fuse to blow on the section in trouble, after which service is restored to the remainder of the circuit by another reclosure. Insofar as quality of service is concerned, this arrangement very nearly performs the duty of a number of circuit reclosers at the cost of only an equivalent number of simple fuses plus that of one breaker at the source. The latter, of course, has a somewhat more elaborate control arrangement and may need to be heavier. However, in some situations there may be appreciable net savings in first cost, and the substitution of a single piece of automatic equipment with moving parts for a number of such devices may reduce greatly the problems of maintenance. The scheme has been applied successfully to circuits consisting individually of 100 miles of line or more, gap-protected. It is in use both in urban and in rural areas. Its possibilities are well worth investigating for application in specific situations.

Lee M. Moore and Max W. Rothpletz:* The authors are highly gratified to acknowledge the interest shown in this paper by rural power engineers and electrical-equipment design engineers, as evidenced by the interesting, competent, and constructive discussions.

These discussions indicate that there are a number of side roads in the design and application of oil circuit reclosers which should prove interesting and valuable on being investigated further. However, such investigation must wait until personnel again is available for that purpose.

In order to indicate avenues which further research might follow, the authors are happy to present their opinions respecting certain portions of the presented discussions.

Inasmuch as the survey covers a relatively limited period of time, no definite conclusions should be reached regarding the estimated maintenance and operating savings. These savings, however, as Mr. Wyman indicates, appear to be very substantial, and it is hoped that future maintenance costs on the oil circuit reclosers will increase these savings.

Regarding continuity of service on rural power-distribution circuits, it is believed that the quality of service to rural users ought to be equal to or better than the quality of service now rendered city and urban consumers. The reason is that lengthy interruption of service to some rural loads, for example, incubators and brooders, results in direct financial loss rather than mere inconvenience.

In answer to Mr. Clarke's question, the momentary interruption in service when a recloser operates has not to our knowledge created any serious problems for rural consumers. In this connection, however, it is reasonable to assume that momentary interruptions to rural consumers are not of as much importance as they are to large power consumers commonly served by utility systems. The length of outage which would result in definite rural-consumer inconvenience has not been determined but would depend, of course, on the type of appliances or equipment being served.

Mr. Rawlins points out that the suggested specifications for small oil circuit reclosers "seem both desirable and feasible." This statement is significant and of considerable importance in that it was our thought to present some definite objective for the designers to shoot at.

Breaker operations on systems with arrester- and gap-protected transformers were tabulated in the paper separately to show the operation of these reclosers with different imposed operating duty. In these tabulations, CSP transformers were classified as arrester-protected transformers. At present there are not enough data available to indicate the definite superiority of one type of system protection over the other. Since there is no "typical" or "average" REA-financed system because of the effects of such factors as terrain, weather conditions, geographic locations, and others, any study aimed toward proving the superiority of gaps or arresters for rural line protection should include systems which operate under every conceivable condition.

The large number of breaker operations shown in Table II of our paper for the months of September and October should not be considered unusual in view of the fact that about 75 per cent of the REA-financed systems are located in Midwestern and eastern-seaboard states. By referring to the isokeraunic maps contained in the *Monthly Weather Review*, published in May 1935, by the United States Department of Agriculture, Weather Bureau, Washington, D. C., it may be noted that lightning storms are quite prevalent in these areas during the months of September and October. This, of course, explains only the total number of recloser operations and not the unequal division of operations between gap- and arrester-protected systems.

The subject of Mr. Beck's question, though mentioned before, bears further emphasis, for it is reasonable to expect that the quality of service to rural consumers must be improved and approach uninterrupted service insofar as is practicable. Rural power engineers should consider this specific item as the first order of business. We think that the suggested reduction of 25 per cent in the cost of reclosers and lightning protection is a step in the right direction. However, we must not permit ourselves to become overly satisfied with available equipment, but should continue to strive for improved and more economical ways of doing the job.

Mr. Andrews touches on possibly one of the most difficult and important problems involved in the design and application of these small reclosers; that is, their co-ordination with transformer-primary and short-tap sectionalizing devices. Although considerable thought has been given to this problem, and progress is being made toward its solution, there is much work left to be done in this direction.

Bullard, Mosley, and Treadway have presented valuable comments on the proposed instantaneous- and time-delay sequence of operations of oil circuit reclosers. While we have not had actual experience with small reclosers embodying this operating sequence, we believe that it would provide definite advantages where co-ordination with fuses on the load side of the reclosers is required.

A New High-Interrupting-Capacity Fuse for Voltages Through 138 Kv

Discussion and author's closure of paper 43-103 by H. H. Fahnoe, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 630–5.

Jerome J. Taylor (Detroit Edison Company, Detroit, Mich.): This paper contains much valuable material, of both general and specific nature, in regard to fuse designs and applications. If information is available for publication it would be of additional interest to know whether actual fuse-tube pressures and hinge-support reactions were measured, what the stroke of the arcing rod is for the voltage ratings mentioned, what clear space is required at the muzzle end of the device, and what actual fault currents are capable of causing destruction of these fuses.

Competition between designers of fuses and designers of circuit breakers recently has taken the turn, recurrently true of engineering problems generally, that each group has been making use of design features previously assumed to be characteristic of the other. Thus, the present trend in circuit-breaker design is toward the use of blasts of gases or liquids, while, at the same time, fuse designers have been employing shunted springs as auxiliary separating devices, and now, as described in this paper, solid-retractable arcing rods.

As compared to circuit breakers, fuses have been traditionally inadequate in regard to circuit reclosure and require careful manipulation in order to take part in co-ordinated relaying. On the other hand, their capital cost is relatively small; their interrupting range and reliability have been increased greatly; speed of interruption is unmatched; and peak fault current is often limited to values substantially less than those corresponding to circuit impedance. These tendencies have resulted recently in British proposals to use combinations of breakers and fuses in series, the breakers handling switching and medium faults with the fuses timed to clear heavy short circuits in advance of the breaker. There seems some basis for thinking that a fusible conductor has inherent advantages over separable contacts in clearing heavy faults, and it would be interesting in this connection to know whether the author thinks his design is adaptable to duty in circuits of short-circuit capacity in excess of 1,000,000 kva. It may be that in the future a hybrid design of interrupter will be developed, embodying the special abilities of several present types.

Regarding fuse construction, there always has been a fairly definite grouping into enclosed and expulsion types. Another differentiation seems to be becoming more pronounced—one style employs fillers or liners containing hydrates; the other uses relatively inert materials, such as sand or granulated quartz. Fundamental research has shown that arc drop in steam is higher than in any other gas except hydrogen.[1] It is this fact that appears mainly responsible for the ability of fuses of the gypsum- and boric-acid type to interrupt relatively high voltages per inch of length, assuming a given wire size of fusible element. This ability is extended by pressure and turbulence, giving rise to a new difficulty encountered and solved by the author of the paper, that the effective dielectric strength of an arcing chamber may exceed that of the external air.

For heavy power applications, it seems possible that in special cases, liquid cooling of the main conductors by forced convection would be advantageous, both in fuses and in some types of breakers. It would entail additional costs and complications but offers potential profits in that load ratings up to ten times present figures might be obtained in an interrupter of the same physical size and short-circuit capacity.

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* Max W. Rothpletz is associate engineer with technical standards division, Rural Electrification Administration. In view of the fact that coauthor of the paper, B. O. Watkins, is now a lieutenant, junior grade, United States Naval Reserve, Mr. Rothpletz kindly consented to act as an alternate in preparation of this discussion.

REFERENCE

1. **C. G. Suits.** *Journal of Applied Physics*, volume 10, March, September, 1939; *General Electric Review*, volume 39, 1936, page 194, and volume 42, 1939, page 432.

C. L. Schuck (General Electric Company, Philadelphia, Pa.): Mr. Fahnoe's paper gives a very rational development of the economic and engineering aspects leading to the desired features in fuses for the higher voltages. The new fuse described necessarily must be classed as a vented fuse, since an external expulsion of arc products accompanies interruption. It should be noted that, in addition to the water vapor resulting from the breakdown of boric acid, fuses of this type also expel quantities of incandescent gas resulting from the explosion of the metal forming the current responsive element together with air which has been heated to incandescence by the arc. The prevention of the expulsion of solid matter, such as the fuse cable in conventional expulsion fuses, is praiseworthy and may reduce the degree of violence of the disturbance accompanying interruption.

The problem of installing and removing high-voltage fuse units by the operator is recognized in the paper, and, in this connection, it should be noted that the average man is capable of handling about 150 foot-pounds of the kind of effort represented by a weight, such as a fuse, at the end of an arm, such as a switch hook. It is estimated, from the construction of the 138-kv fuse unit described, that such a fuse unit would weigh in the neighborhood of 15 pounds. This would indicate a ten-foot switch hook as the maximum practical for handling a 138-kv fuse unit. Allowing four additional feet for the effective height of the operator, this places the maximum height of the upper insulator stack of the fuse disconnecting switch at 14 feet above the ground. The lower end of the fuse unit is, therefore, eight feet above ground, a height which is considered inadequate for this voltage, and, obviously, an operating platform is required. Even with the operating platform, is it considered good operating practice to allow personnel to come within eight feet of a live 132-kv device? If not, a disconnecting switch is required to isolate the fuse before it can be handled. It also would be of interest to know whether the reduction in violence of interruption by the use of the fuse described permits mounting this close to ground.

H. H. Fahnoe: In the new high-voltage fuse the interruption of heavy fault currents is initiated by the fusion of an extremely short calibrated-fusible element, and the resultant arc is drawn out by separable contacts, as indicated by Mr. Taylor. On high short-circuit currents, where extremely fast clearing is desirable, the fuse element usually melts during the first half-cycle, and interruption is completed quickly by the lightweight retractable arcing rod being drawn through the interrupting chamber. The length of this boric-acid-lined interrupting chamber is shown in Table I. The use of separable contacts, instead of a long fusible conductor, greatly reduces the arc energy in the fuse.

Table I

Fuse Unit (Kv)	Interrupting Chamber (Inches)
138	36
115	31
92	26
69	17

It is probable that fuses of this type can be made having an interrupting capacity in excess of 1,000,000 kva. However, the field for such fuses is not yet clear. It is desirable to explore the possibilities of applying the present fuse before developing a fuse of increased interrupting capacity.

The interrupting tests made on these fuses included tests to destruction. The fault currents required for failure vary with circuit conditions. Actual fuse-tube pressures and reactions were not measured because of the difficulties of making such measurements at the potentials involved.

The problems of installing and operating high-voltage fuses, as outlined by Mr. Schuck in his comments, deserve the consideration of all fuse designers. However, Mr. Schuck apparently has overlooked the fact, that, in placing the fuse unit in the mounting, one end of the fuse unit must engage the lower contact. This, according to Mr. Schuck's figures, would place the lower end of the mounting at 14 feet above ground. It is relatively easy to rotate the fuse about the hinge point to the closed position.

The cone of incandescent vapor ejected from the fuses when interrupting maximum fault current is approximately 72 inches long. Since these higher-voltage fuses often are mounted on greater pole spacing in an open structure, the expulsion of incandescent vapor is not so important as in the case of lower-voltage fuses.

It would not be considered good operating practice to allow personnel to operate these high-voltage fuses without isolation from live lines, particularly as hook-stick-operated switches do not have the ability to interrupt any magnitude of load or magnetizing current.

Safe Ratings for Overhead Line Conductors

Discussion and author's closure of paper 43-85 by Leonard M. Olmsted, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, pages 845–53.

L. F. Hickernell and A. A. Jones (Anaconda Wire and Cable Company, Hastings-on-Hudson, N. Y.): While the various data and formulas presented in this paper are not new, the particular combination of them to form the analytical method presented by the author appears to be novel. The methods and formulas used in the various steps of the analysis are well established and need no comment. However, some of the basic data dealing with annealing characteristics of copper are not so well established, and specific comments may be warranted.

Any analytical method based on fundamental data derived from various sources involves the inherent danger that these data may not be compatible. If all details regarding test methods and test samples used to obtain these data are not thoroughly scrutinized, it is very possible that inaccurate conclusions may be drawn. This is particularly true in the case of annealing characteristics of copper.

The author states that the rate of annealing depends (among other things) upon the purity of the copper. This is quite true, but it also depends to an even greater extent upon the nature and combination of impurities present, even in minute quantities. Commercial electrolytic tough-pitch copper having approximately the same purity as determined by chemical analysis (that is, copper, 99.95+ per cent) will vary tremendously in annealing characteristics, depending upon the characteristic impurities of the ore source from which it was derived. As an example, medium hard-drawn copper wire annealed at 150 degrees centigrade so as to give a reduction of 10 per cent in tensile strength required only about two hours for one sample but 400 hours for another.

Similarly, referring to Figure 3 in the paper, variations of tensile strength with per cent cold drawing are to be expected. Tests made by our company on a representative commercial tough-pitch copper indicate tensile strength of approximately 59,000 pounds per square inch for 50 per cent reduction, and 64,000 pounds per square inch for 75 per cent reduction. These represent an appreciable increase in the values presented in Figure 3.

Referring to Figure 4, the data presented are presumably derived from short-time high-temperature anneals such as used in the industry. They indicate 30 per cent annealing in one per cent of time for complete annealing. Tests made by our company on a representative commercial tough-pitch copper annealed at 150 degrees centigrade indicate a seven per cent reduction in tensile strength (or approximately 14 per cent annealing) in one per cent of time for complete annealing in the case of hard-drawn wire and no measurable reduction in the case of medium hard-drawn wire. In order to obtain a seven per cent reduction in tensile strength on medium hard-drawn wire, approximately four per cent of time for complete annealing was required.

The author expresses the hope that "carefully controlled tests can be run to establish the time–temperature data for hard-drawn transmission conductors definitely in order to make available their full capacity." This investigation is now under way in our laboratories. Unfortunately, such tests require a long period of time and, hence, have not progressed sufficiently to warrant the results being presented at this meeting. Until these data are available, we prefer to postpone any discussion of Figure 7.

D. D. Clarke (Kansas City Power and Light Company, Kansas City, Mo.): The author emphasizes the limiting factor of clamps, connectors, and joints on the conductors, especially those caused by the oxidation of the contact surfaces with resulting overheating; but he has given us very little discussion on, or hope of, relief from this factor. Both Mr. Olmsted's paper and the working group report given by Mr. Hansen are in line with the heavy loads we are asked nowadays to put on overhead copper conductors, but there was very little said with regard to preserving the conductivity of the joints, which, because of oxidation, may really become the limiting factor in loading the line.

The company with which I am associated has for many years made a practice of protecting all conductor joints against oxidation. This practice started in 1911 and became more effective as experience was gained with the results. Our operators believe that this practice of protecting all conductor joints against oxidation has saved us an untold amount of trouble, especially when our conductors are forced to carry heavy loads. The small cost of an oxidation-preventative coating in the joints is entirely incommensurate with the considerable cost of repair that we experience on joints that have not been so protected.

L. R. Gaty (Philadelphia Electric Company, Philadelphia, Pa.): This paper is an excellent addition to a subject about which there are still some uncertainties. It is particularly important to obtain additional data on the relations of temperature, time, and annealing. If these relations can be established reasonably well, a major step forward will have been completed.

There are two definite problems that should not be confused:

1. Normal rating of the conductor. The limitations of present available information indicate a need for conservation in establishing normal ratings. Voltage regulation will be the principal limitation in many instances.
2. Determining the conductor rating for emergency loads, the infrequent occurrence and short duration of which make it unnecessary to be quite so free with generous factors of safety.

There are several factors involved in any determination of conductor ratings, and the author has introduced some margin of safety at nearly every step. He uses an emissivity factor of 0.5, which is slightly low for well-oxidized copper. The author determines equivalent ambient temperatures by a method which is conservative and then allows also for some increase in wire temperature due to the effect of solar heating, which is present only on clear days. Convection is calculated at wind velocities which are discounted arbitrarily to recognize some possible effects of shielding and axial air motion in the conductor plane, but ignore the vertical components of air motion which are crosswise at all times, the cooling effects of which are predominant outdoors whenever the horizontal-crosswise component of wind velocity is small. Temperature ceilings are selected conservatively. When one considers these various factors and the improbability that the maximum possible emergency load will be coincident with critical atmospheric conditions, it appears that factors of safety have been pyramided with exceptional generosity.

In attacking the problem faced in Philadelphia, the Weather Bureau records of simultaneous wind and ambient observations were used as a basis for determining the approximate effect upon conductor heating for many years. The atmospheric conditions which would produce the maximum possible conductor temperature have occurred on an average of six nonconsecutive hours per year, or so infrequently as to have been considered unlikely to coincide with the hours when it might be necessary to operate the conductor at its emergency load limit. Few emergencies occur at the time of maximum load; hence, the load probably would not be nearly equal to the emergency load limit of its conductor, even if an emergency should occur at the time of a critical atmospheric condition.

Operations at the emergency load limits in Philadelphia will produce a maximum hour conductor temperature of 85 degrees centigrade on the average summer day and less than 135 degrees centigrade in all but the six most critical hours when it is possible, but very improbable, that emergency operation might carry the temperature up to a ceiling of 175 degrees centigrade. These conditions appear to be reasonably acceptable for emergency load ratings. Emphasis should be placed on checking sags and clearances for whatever ceiling temperature is decided. A 66-kv line recently checked showed that contact would occur at two locations if the conductor temperature should reach 135 degrees centigrade. The limitations of clamps, fittings, and terminal equipment are also important.

E. Hansson (Pennsylvania Water and Power Company, Baltimore, Md.): The author is to be congratulated on his thorough analysis of the problem of rating overhead conductors. I am particularly pleased to see him ending his discussion with a word of caution. There is another reason, not mentioned by the author, why such caution is necessary. It is mentioned in the interim report presented at this meeting and may be worth elaborating to some extent.

In making the tests on the heating effect of clamps, as mentioned by the author, it was found that the core strand of a seven-strand 2/0 copper conductor reached a temperature 17.5 degrees centigrade above that of the surface of the conductor at a 425-ampere current. In order to confirm this, further tests were made on 7- and 19-strand 4/0 conductors. In both cases the difference between center strand and surface was 16.9 degrees centigrade at a current of 490 amperes. Another check on a 300,000-circular mil all-aluminum conductor gave a difference of 7.2 degrees centigrade at 375 amperes. If these values are placed on the basis of identical temperature rise, 60 degrees centigrade, the differences are substantially the same, varying from 10 degrees for the 300,000-circular mil aluminum to 13 degrees for the 2/0 copper. No such difference was found in the original test on the 795,000-circular mil steel-reinforced aluminum cable, the measured difference between inside and outside layers being 3.2 degrees centigrade at 920 amperes.

The rather meager data seem to indicate that the gradient in a homogeneous conductor is considerable, but that it is practically absent in steel-reinforced aluminum cable. The gradient seems to be proportional to the temperature rise, and the values given are for rather high currents, but, even for such values of current as may well be used for the conductors in question, the difference will be ten degrees centigrade or more. Considerable more test data will be required before any definite values can be established for the various conductor sizes, but it is apparent that the effect must be considered when establishing maximum ratings.

Leonard M. Olmsted: The several objectives of the paper were to take the fullest possible advantage of the limited information already published in engineering literature, to indicate those phases which most need investigation, and to provide a procedure by which additional data may be applied as they become available. It is fortunate that the subject may be divided into components to be investigated independently, as that approach permits simultaneous work by a number of investigators to expedite the ultimate determination of the whole. The four discussions presented indicate that such independent investigations are being made and lend encouragement to the hope that standardized ampere ratings may be prepared for open-wire conductors comparable with tables now available for underground cables.

Mr. Hickernell and Mr. Jones have emphasized the marked variations in the behavior of commercial hard-drawn and medium-hard-drawn copper wire. The data they give are typical of those from previous publications which were the only information available from which to develop conductor ratings. Nothing can be accomplished by attempting to compare these few additional data with the paper until sufficient tests have been conducted and analyzed to justify preparing entirely new curves to replace those of the paper. It is most encouraging to know that an extensive series of tests is now under way, as it is reasonable to hope that the resulting data will reduce materially the probable errors in one of the most important components in the derivation of safe conductor ratings.

Mr. Hansson calls attention to the variations in temperature measured at various points in a stranded conductor. It does not seem probable that higher temperature in the core strand of a stranded copper conductor is serious, however, as the tensile characteristics of the conductors depend principally upon the time–temperature exposure of the other strands. Steel-core conductors apparently are almost free from this temperature difference, and are inherently less affected because of the greater allowable temperature for the steel. The fact remains, however, that considerable care is necessary in testing the temperatures of conductors and associated equipment if the results are to be dependable.

Mr. Clarke's discussion of connector and joint heating is an important contribution to the subject. This phase was only mentioned in the paper, however, because it appears that the contact surfaces of the connectors should be cooler than the conductor alone, and the conductor temperature limits established by annealing of hard-drawn copper are so low that oxidation of the contact surfaces seems improbable. Other conductors with higher temperature limits are more likely to be affected by joint oxidation.

Mr. Gaty's observation of the difference between normal and emergency ratings is appropriate, and the paper indicates a method for allocating the total allowable time–temperature exposure to these two types of service. The author agrees, also, that an effort was made to be conservative at certain points in the paper. The effect of solar heating was included only for the portion of time that sunshine actually exists according to Weather Bureau data. It is difficult to place any particular value upon the vertical components of air movement, but it seems improbable that any appreciable margin of safety has been introduced by neglecting this factor. A correct measure of the degree of conservatism can be ascertained only by a detailed hour-by-hour analysis of coincident atmospheric and current loading conditions during a period of many years, such as has been reported by Mr. Kidder and Mr. Woodward. Such an extensive analysis has not been undertaken by this author, but an investigation of several typical months has shown no perceptible margin of safety other than the 20 per cent mentioned in the final paragraph.

Comparison between the conductor temperatures used by the author and those mentioned by Mr. Gaty is misleading, as the former are intended for hard-drawn copper and the latter for medium-hard-drawn. This difference alone justifies a conductor temperature approximately 28 degrees centigrade higher on medium-hard-drawn copper conductors, giving temperatures comparable with those allowed in Philadelphia.

In conclusion, it is interesting to observe that the Duquesne Light Company has authorized ratings for certain of its lines derived in accordance with this paper. Subsequent experience with suspension clamps has indicated that the 20 per cent margin of safety is none too great until methods are developed to eliminate conductor burning and magnetic heating. Certain tests are being conducted to this end. It is highly desirable that the results of all such test programs be made available in order that this margin of safety may be reduced intelligently to permit the greatest possible use of overhead line facilities.

Interim Report on Emergency Overloads on Overhead Conductors

Discussion of paper 43-105 by the AIEE working group on towers, poles, and conductors of the transmission subcommittee of the AIEE committee on power transmission and distribution, presented at the Cleveland national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, September section, pages 588–90.

L. R. Gaty (Philadelphia Electric Company, Philadelphia, Pa.): A point of considerable interest is that illustrated in Figure 3 of the paper, where it is indicated that the center or core strand of a seven-strand 2/0 copper cable may reach a temperature of 15 degrees centigrade in excess of that at the surface. It is believed that a part of the apparent temperature difference between thermocouples 2 and 3 in Figure 3 of the paper may be due to the difficulties inherent in an attempt to measure surface temperature accurately with a thermocouple. Additional information regarding this temperature difference will be helpful in determining the conductor ratings which may be applied without destructive annealing of the conductor. Possibly, the core temperature, which can be measured without difficulty, should be used to determine maximum ratings.

The tests reported by Kidder and Woodward in their paper[1] stated that the copper temperatures were observed by a thermocouple that was inserted between the core strand and the outside layer of seven-strand 2/0 copper.

Tests of magnetic heating in the standard-suspension and dead-end clamps, used by Philadelphia Electric Company with 2/0 copper, have shown that the maximum temperature in the clamp is somewhat lower than the temperature of the conductor at all loads up to 560 amperes. The original tests were made in 1933 and were confirmed by recheck in 1943. Variations occur as a result of the type of clamp used, the condition of the conductor, and the size of conductor in relation to the size of the clamp. Care must be used in applying the results of any test to conditions different from those tested.

Reference

1. Ampere Load Limits for Copper in Overhead Lines, A. H. Kidder, C. B. Woodward. AIEE Transactions, volume 62, 1943, March section, pages 148–52.

The Effect of Corona on Coupling Factors Between Ground Wires and Phase Conductors

Discussion and author's closure of paper 43-77 by G. D. McCann, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, pages 818–26.

E. D. Sunde (Bell Telephone Laboratories, Inc., New York, N. Y.): In this paper it is implied that the coupling between the ground wires and the phase conductors is due entirely to electric induction. This is no doubt true when the ground wire carries a negligible current, as in the measurements on short wires described in the paper. However, in the case of a lightning stroke to a transmission-line tower or to a ground wire, the current carried by the latter will cause magnetic induction in the phase conductors. To obtain the voltage of the phase conductors, it is then necessary to consider magnetic, as well as electric, induction.

Consider a ground wire and a phase wire of such length that reflections need not be considered, at least in calculating the crest voltage. The coupling factor, or the ratio of the voltage of the phase conductor 2, to that of the ground wire 1, is then

$$K=S_{12}/S_{11}=(L_{12}/C_{12})^{1/2}/(L_{11}/C_{11})^{1/2}$$
$$=(K_eK_m)^{1/2} \tag{1}$$

where S_{11} and S_{12} are the self- and mutual surge impedances and K_e and K_m the electric and magnetic coupling factors, respectively.

$$K_e=A_{12}/A_{11}=(1/C_{12})/(1/C_{11})$$
$$K_m=L_{12}/L_{11} \tag{2}$$

A_{11} and A_{12} being the self and mutual potential coefficients, L_{11} and L_{12} the self- and mutual inductances.

Since the crest voltage is obtained after a few microseconds, the effective frequency is so high that the equivalent ground plane for the magnetic coupling may be taken at the surface of the earth, as in the case of the electric coupling, so that

$$K_m=K_e$$

and

$$K=(K_eK_m)^{1/2}=K_e \tag{3}$$

This relationship is true only when K_e is the electric coupling factor without corona. The magnetic coupling factor is not affected by corona, because the conductivity of the corona is not large enough to affect the inductances L_{11} or L_{12}. If K_c is the coupling factor with corona, as determined in the paper, and $K_m = K_e$ the electric coupling factor without corona, then the coupling factor for an actual line is

$$K=(K_eK_c)^{1/2} \tag{4}$$

That is, the coupling factor to be used on an actual line is the geometric mean of the coupling factor K_c with corona which is obtained from the measurements in the paper, and the coupling factor K_e without corona, which may also be obtained from the results of the measurements, or may be calculated by the usual formulas.

As a first approximation, it is usually permissible to take the arithmetic mean $K = (K_e + K_c)/2$ instead of the geometric mean as in equation 4. If using the formulas for K_e and K_c, it then follows that the effective radius to be used in calculating the coupling factor for an actual line is the geometric mean of the actual radius and the equivalent corona radius of the ground wire, the latter being given in the paper for various conditions.

In the preceding discussion, reflections in the ground wire caused by the presence of tower grounds along the ground wire were not considered. Such grounds will reduce the voltage and increase the current in the ground wire, so that magnetic coupling will tend to predominate. The coupling factor will then be smaller than that obtained from equation 4 and will approach the magnetic coupling factor, or the electric-coupling factor without corona.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): As usual, Doctor McCann has made a very thorough investigation of this problem. The question is whether these results are applicable to actual transmission practice. The question of insulator string flashover on towers with low tower-footing resistance has long been a puzzling one. There are numerous instances recorded in the literature where the tower potential is of the order of 80 per cent to as low as 10 per cent of the spark-over voltage based on a 1.5x40 wave, and the *IR* drop across the tower ground resistance. Now, Doctor McCann's tests show that the coupling factors should be from 30 per cent to 100 per cent greater than the purely electrostatic coupling factors. While there are many other explanations for such flashovers at apparently low tower voltages, such as direct strokes to the wire, the principal ones responsible probably are higher short-time impulse resistance of grounds, the inductive drop in the tower proper and in the ground connections, such as counterpoise and ground rods. The effect of these factors, as well as the repeated reflections from nearby towers, is to produce highly peaked voltage waves at the tower, and, therefore, the ground wire would require a still higher voltage across the insulation than that caused by a 1.5x40 wave. From this point of view, it is very difficult to believe that the coupling factors during an actual stroke to a tower are really as high as obtained during Doctor McCann's tests.

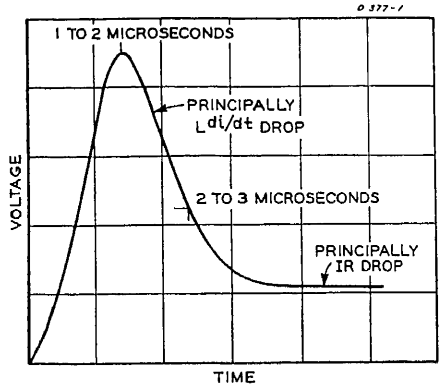

Figure 1. Typical ground-wire voltage for lightning-stroke currents of steep fronts

A considerable difference for wave shapes of the type shown in Figure 1 of this discussion, where the crest is reached in one to two microseconds, and the wave decreases to half value in two to three microseconds, should be due to two factors:

1. The starting voltage of visible corona, and, consequently, the corona radius on a wire, have a time lag similar to that of the breakdown voltage. The corona shown in Figure 13 and Figure 14 of the paper is due to 1.5x40 waves, where a time interval of from four to ten microseconds is available for formation and expansion of the streamers.

2. On a long line, the surge-impedance effect of the line enters into discussion as soon as a traveling wave is formed on the ground and line conductors with currents of thousands of amperes. In the tests, the line conductor acts as a kind of capacitance-divider pickup device.

There seems to be a question of a race between the time to form the corona on the wire and the speed of travel of the induced wave on the line conductor. For slow fronts this is not important; however, for fronts of the order of one to two microseconds, the increase in coupling factor caused by corona should be considerably less than that obtained in the tests described. Direct-stroke measurements on the Empire State Building have shown that 50 per cent of the current fronts are one microsecond or less. Similar steep fronts are to be expected on transmission lines. It would appear necessary to make further tests to represent more nearly actual stroke conditions.

It might be well to insert in the paper a word of caution to what extent the curves of Figure 18, in conjunction with Figure 16 and Figure 17, can be used. For instance, for the 30-foot ground wire, potentials on the line wire can be obtained which are greater than the ground-wire potential. Furthermore, the line-conductor potential attains a maximum at around 1,000,000–2,000,000-volt ground-wire potential.

Doctor McCann does not state to what extent the line conductor produces corona and what effect such corona would have on the corona formation on the ground wire. In particular, on the tests with the ten-foot-high ground wire, where coupling factors have been obtained of the order of 0.8, it would seem that considerable corona should have been observed on the line conductor. With two wires under corona conditions, the capacitive coupling would become more nearly that of two cylinders of considerable diameter. Perhaps this accounts partly for the fact that the equivalent corona radius is about one half of the observed streamer length.

Leonard M. Olmsted (Duquesne Light Company, Pittsburgh, Pa.): In his discussion of coupling between ground and counterpoise wires and phase wires, Doctor McCann has mentioned the installation described in a previous publication by E. R. Whitehead of the Duquesne Light Company. It is interesting to note that the 66-kv double-circuit steel-tower line with single overhead ground wire, to which the counterpoise wire has been added, is showing an improvement in reliability, even beyond our expectations.

G. D. McCann: Mr. Sunde has raised a very fundamental point regarding the relationship between the measured coupling factors, given in this paper, and the total coupling as produced by both electrostatic and magnetic effects. The measured coupling factors represent only electrostatic coupling. However, it does not appear that the equations given by Mr. Sunde are rigorously correct, even for the assumptions he made. These were that all distortion and losses such as those caused by corona are neglected. It is assumed that the capacitances can be determined from the effective ground-wire corona radii, while the inductances are determined from the true radius of the ground wire. It is not known to what extent these assumptions are justified. Also, the author has, as yet, been unable to obtain a solution based upon these assumptions which, for the case of aerial conductors, gives the relation between the induced-conductor potential and ground-wire voltage in the form of a purely numeric factor. Such a solution can, however, be obtained readily for the simpler case of concentric cylinders for which the coupling-factor equations of Mr. Sunde should also apply.

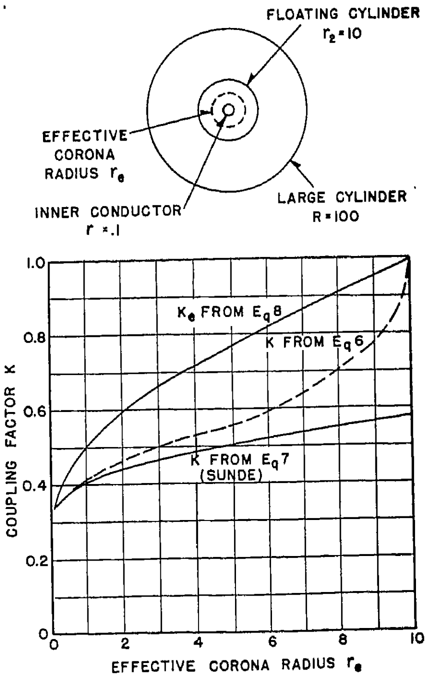

Figure 2

Consider the case illustrated in Figure 2 of this discussion in which the ground wire is represented by the inner conductor of radius r_1, the phase conductor by the floating cylinder of radius r_2, and the ground plane by the large cylinder of radius R. Let it be assumed that the system of cylinders is semi-infinite in length, and that a voltage is applied between the inner conductor and the ground cylinder at the one end. For this case the voltage induced on the floating cylinder in terms of the inner conductor potential is given by the coupling equation:

$$K=\frac{Z_2}{Z_1+Z_2} \tag{1}$$

where Z_1 is the surge impedance for the system consisting of the inner conductor and floating cylinder. Z_2 is the surge impedance for the floating and outer cylinders. Three cases will be considered:

CASE 1. NO CORONA, DIELECTRIC CONSTANT EQUAL TO 1

For this case

$$Z_1\alpha \log\frac{r_2}{r_1};\qquad Z_2\alpha \log\frac{R}{r_2}$$

$$K=\frac{\log\dfrac{R}{r_2}}{\log\dfrac{R}{r_1}} \tag{2}$$

From equation 1 of Sunde's discussion

$$K=\sqrt{\frac{L_{12}C_{11}}{L_{11}C_{12}}}$$

The equations for these constants are the following:

$$L_{11}\alpha \log\frac{R}{r_1};\qquad L_{12}\alpha \log\frac{R}{r_2};$$

$$C_{11}\alpha \frac{1}{\log\dfrac{R}{r_1}};\qquad C_{12}\alpha \frac{1}{\log\dfrac{R}{r_2}}$$

These give the following equations for the coupling:

$$K=\sqrt{\frac{\log\dfrac{R}{r_2}\times\log\dfrac{R}{r_2}}{\log\dfrac{R}{r_1}\times\log\dfrac{R}{r_1}}}=\frac{\log\dfrac{R}{r_2}}{\log\dfrac{R}{r_1}} \tag{3}$$

For this assumption, Sunde's equation checks that derived from the surge impedances.

Case 2. Medium Between Inner Conductor and Floating Cylinder Having Dielectric Constant k

For this condition Z_1 is changed to the following:

$$Z_1 \alpha \frac{1}{\sqrt{k}} \log \frac{r_2}{r_1}$$

$$K = \frac{\log \frac{R}{r_2}}{\log \frac{R}{r_2} + \frac{1}{\sqrt{k}} \log \frac{r_2}{r_1}} \qquad (4)$$

For Sunde's equation

$$C_{11} = \frac{1}{\log \frac{R}{r_2} + \frac{1}{k} \log \frac{r_2}{r_1}}$$

$$K = \frac{\log \frac{R}{r_2}}{\sqrt{\log \frac{R}{r_1}\left(\log \frac{R}{r_2} + \frac{1}{k} \log \frac{r_2}{r_1}\right)}} \qquad (5)$$

The equation given by Sunde does not check for this condition. The difference is greater the higher the dielectric constant. However, for values of k no greater than 4 or 5, the difference between the two solutions is usually not very great.

Case 3. Dielectric Constant One; Effect of Corona on Inner Conductor Represented by Corona Radius r_e

For this case Sunde's assumptions, that capacities are given by the effective corona radius r_e and inductances by r_1, will be used.

This gives for the surge impedance Z_1 the following:

$$Z_1 = \sqrt{\frac{L_{11}}{C_{11}}} \alpha \sqrt{\log \frac{r_2}{r_1} \times \log \frac{r_2}{r_e}}$$

Then from Equation 1

$$K = \frac{\log \frac{R}{r_2}}{\log \frac{R}{r_2} + \sqrt{\log \frac{r_2}{r_1} + \log \frac{r_2}{r_e}}} \qquad (6)$$

For Sunde's equation

$$L_{11} \alpha \log \frac{R}{r_1}$$

$$C_{11} \alpha \frac{1}{\log \frac{R}{r_e}}$$

$$K = \frac{\log \frac{R}{r_2}}{\sqrt{\log \frac{R}{r_e} \times \log \frac{R}{r_1}}} \qquad (7)$$

The electrostatic coupling for this case is given by the equation

$$K_e = \frac{C_{11}}{C_{12}} = \frac{\log \frac{R}{r_2}}{\log \frac{R}{r_e}} \qquad (8)$$

To illustrate the difference in equations 6, 7, and 8, calculations were made for the conditions illustrated in Figure 2 of this discussion. As shown by the three curves of this figure, all three equations (as they should) give the same result when r_e equals r_1. When r_e equals 10 or r_2, the coupling factor should be 1, as correctly given by equations 6 and 8. However, the curve derived from Sunde's equation lies somewhat below the curve which should be correct for the given assumptions (equation 6) and never reaches one. It starts to diverge rapidly at r_e equals about 6.

Over the principal range of ground-wire potentials, the measured effective corona radii as shown by Figure 15 of the paper do not exceed about five feet. Thus, for most transmission lines, the effective corona sheath does not bridge more than about half the spacing between the ground wire and phase conductors. The curve of Figure 1 indicates a fairly close agreement between Sunde's equation and equation 6 over this corresponding range (up to r_e equals 5). It thus appears probable that the use of Sunde's equation, as given in his discussion, together with the measured electrostatic coupling factors in the paper will give values of the initial induced voltages that are only about 10 or 20 per cent low.

Further consideration is being given to this problem. It is recognized that there are other factors determining the surge voltages produced on phase conductors which have not been given consideration in the past. Most important of these are the effects of the electrical conditions of the stroke channel itself. It was for this reason that no attempt was made in the paper to determine directly transmission-line flashover conditions.

Guide for Wartime Conductor Temperatures for Power Cables in Service

Discussion and closure of paper 43-104 by the Association of Edison Illuminating Companies subcommittee on wartime temperatures for cable circuits, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, September section, pages 606–10.

H. L. Davis, Jr. (Philadelphia Electric Company, Philadelphia, Pa.): Past operating experiences of the utilities have been a major contribution in paving the way for the increased operating temperatures which have been outlined so thoroughly in this guide. While the new temperature ceilings given are feasible, the guide points out very clearly what the operating effects might be and the conditions under which they may be justified. Experience in Philadelphia with cable operating at temperatures above the normally recognized peacetime limits, confirms the discussion in section 2.4 of the guide, "Assumed Influence on Cable Life."

For example, operation of 22 miles of three-conductor 13-kv belted cable at full peacetime rating resulted in a failure rate which was three times the system failure rate. When the copper temperature on this small group of cables was increased from 77 degrees centigrade to approximately 100 degrees centigrade, the failure rate increased to 11 times the original system rate (six per 100 miles). However, the system failure rate only increased 50 per cent. It is obvious that no system is exposed to general load increases, but only limited parts of a system will be effected by the war demands. Consequently, while 400 per cent increase in failure rate, referred to in the guide, may seem to be exorbitant, it will involve an over-all failure rate that should be well within the ability of the maintenance personnel to handle without an undue influence on continuity of service.

The cable temperatures of about 100 degrees centigrade experienced in Philadelphia did not produce an appreciable number of dielectric failures. The trouble has been mostly that of openings in the lead sheath, which caused about 83 per cent of the failures on the heavily loaded lines. At the higher temperatures the lead sheath is very much weaker and therefore scoring, minor electrolysis attacks, and mechanical injuries to the sheath are hastened to failure by the higher loading.

In connection with the statement in the guide that increases in failure rate will not become apparent in less than six months or a year, it is to be noted that the presence of the sheath cracks, which may have required six months or a year to produce, will be brought quickly to failure only if the cable continually is submerged. Where the ducts are dry, experience has shown that cables with open sheaths may operate for several years before failure.

Robert J. Wiseman (The Okonite Company, Passaic, N. J.): The recommendations of the Association of Edison Illuminating Companies subcommittee on wartime temperatures for cable circuits, as reported to Mr. Halperin, are of interest to us in that the committee has set up not only tables indicating temperatures at which it believes a cable may operate, but also warning signals, or "red flags," indicating what one can expect if a cable load is increased to a value that results in these temperatures: namely, a reduction in the over-all life of the cable, whether insulation or sheath, and increased cable movement (therefore mechanical effects on the lead, and possible effects on accessories such as joints, reservoirs, and terminals)—all resulting in an increase in failure rate. I would like to recommend that the utility engineer who is contemplating taking advantage of this emergency loading of cables read the nine papers given at the AIEE national technical meeting in January 1943, in New York.* These papers tell in detail the views of the utility engineer and the manufacturer about overloading of cables. I understand that the authors took into consideration the views expressed in these nine papers when preparing the guide.

The emergency temperatures set up are presumably based on the experiences of some companies. I believe that under closely observed control it may be safe to reach these temperatures for impregnated-paper cables. I am not so sure about the values for varnished-cambric cables. I think they are high, particularly for voltages over 7.5 kv when dielectric losses are of importance. The emergency temperatures for code insulation are too high, unless it is expected and accepted that a very short life will result. The values for ozone-resistant compounds of the oil-base type can be raised to 90 degrees centigrade, provided they are not subject to a high compression, such as a clamp, during the emergency temperature period.

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* All nine papers are available in a single pamphlet (Special publication 6), in limited quantities, from AIEE headquarters at 75 cents per copy.

I believe that, before emergency loadings are applied to a cable system, a careful survey should be made of the operating record. How does the rated load of the cable compare to the actual load or contemplated load? In the past we have been conservative in many cases in the rating of a cable. Perhaps the new load is still within the allowable rating of the cable; therefore, there is no emergency loading. If the new load is definitely an overload and the expected temperature is higher than given in this war guide, then means must be taken to reduce this temperature as suggested in the guide, or the load must be reduced.

Although this guide is intended to be used for the duration of the war, it easily can be a guide for future peacetime emergency operation. At that time it will be necessary to review the experiences of those companies that have operated under the guide. I am hoping that all who do so will keep a very complete record of the loadings on their cable circuits, the amount and total time of each emergency loading, and the record of cable failures or cable replacements. Unless this information is available, it will be hard to convince some of us that emergency loads have been carried and that the cables have given no trouble. The cable manufacturer is just as anxious as the operator to know what is the limit on temperature operation of a cable. This information can come only from the experience of the operating companies, and they will be asked to present the record as a future guide.

This guide and the nine papers given at the January meeting refer to allowable emergency temperatures, and practically nothing is said about what this means in current loading. The latter is what the operator wants to know when he is called upon to carry extra loads. The permissible load current will depend upon the time the emergency exists, the conditions of installation, cable size, number of cables in duct bank, what load current is being carried at the time the emergency starts, and how many are to carry the emergency load. Some of the large utilities have engineering staffs who have set up their own methods for determining what emergency load current can be carried on a cable. Technical papers have been published recently which give methods for estimating these currents. One by N. B. Walsh of the Duquesne Light Company was presented before the transmission and distribution committee of Edison Electric Institute in Pittsburgh last February; another by A. R. Eaches of the Philadelphia Electric Company, was presented to the AIEE Philadelphia Section; and a third one I gave before the joint meeting of the AIEE Southwest District and the Missouri Valley Electrical Association. In it, I show how it is possible to determine the emergency current under the conditions stated previously from the Insulated Power Cable Engineers Association current ratings, the thermal time constant of a cable, and the time of the emergency. Naturally, a short time such as one hour will permit a higher current than a two-hour or four-hour overloading, and a cable carrying less than its rated load at the time the emergency load starts can carry a larger current than if it were carrying full load.

I believe this war guide, along with the conference papers, is going to be very helpful in guiding the utility engineer as to how far he can go to meet emergency conditions with the knowledge that he is shortening the life of a cable and must accept an increase in the cable-failure rate. A large utility with several cables supplying power between two points and an installation crew available can take advantage of this guide better than one not so well placed. In the latter case the risk may be too costly to take.

C. T. Hatcher (Consolidated Edison Company of New York, Inc., New York, N. Y.): As a member of the AEIC subcommittee which prepared the guide, I wish to bring out that we recognize the fact that we do not know definitely what will occur to systems when the proposed loads are applied. However, we believe that we have taken into consideration all available data and factors and that the proposed loadings are reasonably satisfactory for the conditions under which they are to be applied.

In order to obtain the full value of the use of the proposed loadings, the subcommittee requests that all companies using the proposed loadings maintain accurate records of the operating results, so that they will be readily available for further study. Data obtained in this manner will be of great help in future consideration of allowable operating copper temperatures.

Wm. A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): Mr. Halperin's committee has taken a useful step in preparing this guide to indicate allowable cable temperatures under war conditions. It is unfortunate, however, that the guide expresses its recommendations in terms of temperatures without giving any indication of how such temperatures are to be used.

The temperatures referred to in the tables are defined as "those at the hottest portion of the cable in a given line or circuit."

In the case of cables in ducts, this hot spot will be at the conductor surface and at some considerable distance from the duct mouth. It is evident that the operating engineer has no way to ascertain what is the hottest portion of the cable, and, even if he did, he would be unable to measure its temperature.

This condition is complicated by the fact that the great majority of cables must be laid under such a variety of conditions with respect to thermal dissipation and vintage that they cannot be engineered individually, but must be treated in groups corresponding usually to types only, and sometimes to general installation conditions with uniform rules applying to all members of a group.

The temperatures recommended in the guide have been derived either from practice or from laboratory data. In the former case there must have been some formula whereby hottest-spot temperatures were derived from something that was measured. In that case the authors of the guide would do well to reveal this rule, so that those who want to use it may work the rule backward and derive something measurable from the tabulated unmeasurable temperatures.

However, I suspect that no such rule has been formulated and that the tabulated temperatures have been derived mainly from laboratory work, in which case the hottest spots were obtained by substantially direct measurement. If that is the case, the method of using the guide is anyone's guess.

If I might hazard a guess, the average user of the guide will, in all probability, take the IPCEA tables and increase the current ratings by a factor based on the greater temperature rise and higher final temperature allowed by the guide. For example, 750,000-circular-mil three-conductor-shielded 22,000- or 23,000-volt cable alone in a duct bank, operating at 100 per cent load factor and earth ambient of 20 degrees centigrade has a carrying capacity of 495 amperes according to the IPCEA table, which allows a final temperature of 77 degrees centigrade. Corrected for the guide's hottest-spot temperature of 82 degrees centigrade for normal operation, the current will be

$$495\times\sqrt{\frac{(82-20)}{(77-20)}\times\frac{1+(0.00393\times 57)}{1+(0.00393\times 62)}}=512 \text{ amperes}$$

The same method applied to the 100 degrees centigrade temperature for emergency operation gives a current of

$$495\times\sqrt{\frac{(100-20)}{(77-20)}\times\frac{1+(0.00393\times 57)}{1+(0.00393\times 80)}}=566 \text{ amperes}$$

If the IPCEA carrying-capacity tables had been based upon the most optimistic assumptions of thermal and electrical properties rather than upon the conservative values actually used, a current of 587 amperes would be required to bring the conductor temperature even to the normal 77 degrees centigrade, allowed by IPCEA.

We may, therefore, conclude that *new* paper cables, installed under favorable conditions subjected to currents calculated from the guide temperatures, by this procedure, will not be harmed, because they will not exceed normal operating temperatures. On the other hand, old paper cables and those not favorably situated will attain temperatures which will cause an increase of failure rate and an ultimate lowering of useful life.

The recommendations for varnished cambric are, however, in another category. This material is a very capricious one, and capricious materials are not very reliable when pushed to their limits. That is, however, precisely what the guide proposes to do.

One reason for the capricious nature of varnished cambric is its extremely hygroscopic nature which causes the dielectric loss to vary over a range which is dangerous when considered in connection with accumulative heating. This action may be initiated by high hottest-spot temperatures such as are listed in Table II of the guide. Numerous failures of high-voltage varnished-cambric cable are on record where accumulative heating has been caused either by high ambient temperatures or by heavy loads combined with poor conditions of heat dissipation, neither of which would have affected paper-insulated cables. While the IPCEA tables are conservative enough for low voltages, they leave very little margin of safety for cables rated at over 7,500 volts. I, therefore, consider the recommendations of Table II to be distinctly dangerous.

The recommendations for rubber, in Table III of the guide, are also questionable, particularly for code- and ozone-resistant rubber. The former will not stand 60 degrees centigrade for very long, especially if not protected by a lead sheath.

With regard to ozone-resistant rubber, a series of oven tests on several of the best-known makes shows that they deteriorate quite appreciably even at 70 degrees centigrade and have very short life at 80 or 85 degrees centigrade.

T. H. Haines (Boston Edison Company, Boston, Mass.): The AIEE paper entitled "Guide for Wartime Conductor Temperatures for Power Cables in Service" covers the field quite thoroughly and is particularly valuable for showing the many factors that affect the operating temperature of cables.

In paragraph 2.41 of the guide it is stated that the wartime "normal" temperatures are assumed at least to double the rate of deterioration of the cable as compared with previous practices. It would be of interest at this point, if possible, to state approximately what these rates might be.

In paragraph 3.22 of the guide the effect of racking length and offset of cable on sheath cracking are mentioned, and it is stated that when these values are below the limits of good practice the allowable temperatures should be reduced. Although the limits of good practice must be more or less arbitrary, an approximate table of values would be a helpful addition to the paper. The Boston Edison Company uses the following table:

Cable Size	Minimum Racking Distance (Feet)	Minimum Offset (Inches)
3x1/0 to 3x4/0	9	14
3x300,000 and 350,000 circular mils	10	16
3x400,000 and 500,000 circular mils	11	18
3x700,000, 2x1,000,000 and 2x1,500,000 circular mils	12	20

Section 3.32 of the guide is valuable in emphasizing the importance of temperature surveys in the field. It might be well to mention here the value of soldering thermocouple wires to the sheaths of cable sections during installation in known "hot spots." This permits accurate determination of sheath temperatures, and, hence, conductor temperatures, to be made whenever desired.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): With regard to allusions by Del Mar and Wiseman, to some of the temperatures being excessive, I should like to re-emphasize the points in the last two sentences of the preface of the guide:

(*a*). The use of the temperatures will cause large increases in troubles

(*b*). The temperatures are proposed only as a wartime measure, because their continued use may make the total annual charges for a cable system 20 or 25 per cent, instead of the approximate normal figure of 12 per cent.

Mr. Davis brings out the point, which has been demonstrated elsewhere, too, that day-in-day-out operation at full peacetime temperatures will cause great increases in failure rates. A large majority of the underground circuits in peacetime seldom operates up to peacetime temperature limits, and the country's average rate of cable failures in service of five per 100 miles of cable per year is really based on lower temperatures.

In answer to Mr. Haines' question, continued daily operation at the "normal" temperatures given in the guide might cause a failure rate of 15, 20, or 25 per 100 miles per year for three-conductor 13-kv cable. Usually, more is to be feared from the effects of daily operation at proposed "normal" temperatures than from operation at the proposed temperatures for rare emergencies.

Determination of maximum temperatures to use in wartime is no easy task. Differences of opinion exist as indicated, for example, in the difference of 10 or 15 degrees centigrade for ozone-resistant rubber implied in the remarks of Del Mar and Wiseman. Experience substantiates their implication that the resistance of varnished-cambric cables to temperature varies considerably. However, it may be noted that such insulation, when in apparatus other than cable, is usually given higher temperature limits than in the guide.

It is difficult to frame a short answer to Mr. Del Mar's apparent confusion as to how the temperatures will be used. The proceedings of the American Institute of Electrical Engineers, National Electric Light Association and Edison Electric Institute, as well as foreign publications, contain considerable information as to the determination of the maximum temperatures of a cable circuit. Temperatures found in field experience as well as laboratory data were used in setting the guide temperatures. Of course, the actual "hot-spot" temperatures of a circuit most likely will be a few degrees different from the value as ascertained by the methods available. Likewise, the literature, including present publications, is full of information and argument on methods of determining accurately the "hot-spot" temperature for transformers, motors, and so on.

As recently as this summer, I find, to my surprise, that the temperature limits for wartime conditions in England are still *even less* than the *peacetime* limits of this country. Surely copper, lead, rubber, and so forth are not more plentiful there than here. These facts and others demonstrate that the utilities that started this guide are willing to make, as necessary, unusual sacrifices to save critical materials as a wartime measure.

The discussions at, and prior to, this meeting regarding the guide have been interesting and worth while. However, as indicated in the discussions, the maintenance of accurate and complete records of operating experiences is very desirable from several angles for those cases where cables are operated at, or near, the temperature limits given in the guide.

A Capacitance Bridge for Determining the Ratio and Phase Angle of Potential Transformers

Discussion and author's closure of paper 43-81 by H. W. Bousman and R. L. Ten Broeck, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 541–5.

F. B. Silsbee (National Bureau of Standards, Washington, D. C.): This paper constitutes a very noticeable milestone in the progress of precise measurements in the field of high-alternating voltages. Capacitance potential dividers have been used hitherto with good success in determining the ratio and phase angle of voltage transformers, but in these earlier methods it has been necessary to know with high precision the capacitance as well as the phase angle of the capacitor. The present authors have made the very great contribution in that they have divided the responsibility of the measurement in such a way that the precise determination of ratio depends only upon the values of resistors, which can be measured readily, and which are very constant in time, while the values of phase angle are determined by the air capacitors which are eminently suited for this purpose. Their use of the bridge connection, which is usually called Schering, but which should more properly be called Thomas (as will be seen by the authors' reference 11), has proved to be the key which brings about this much-desired situation.

A number of questions of detail naturally arise in the mind of the reader, and I hope that the authors can supply the answers to these in their closure:

1. What type of detector do they use, and what is its sensitivity?

2. How are the conductances of the many resistors intercompared, and what is the construction of the conductance dials?

3. What voltage is used on the bridge circuit when they obtain their calibrated balance? Presumably, if the voltage were the geometric mean between the primary and secondary voltages of the transformer under test, the current through the resistance arms would be the same as it is when the balance against the transformer is made. Is it necessary to take this precaution?

If, as the authors state, the reference list is to show the present state of the art, it should perhaps include, in addition to references 1, 5, and 10, which give the methods used at Pittsfield, Manchester, and Berlin, those listed at the conclusion of this discussion which describe in similar fashion the methods now in current use at Teddington and Washington.

References

1. Precise Electrometer Method for Voltage-Transformer Testing, R. S. J. Spilsbury.

Institution of Electrical Engineers *Journal* (London, England), volume 67, September 1929, page 1143.

2. Design and Construction of a Shielded Resistor for High Voltages, **R. Davis.** Institution of Electrical Engineers *Journal* (London, England), volume 69, August 1931, page 1029.

3. A Shielded Resistor for Voltage-Transformer Testing, **F. B. Silsbee.** Scientific papers of the National Bureau of Standards, volume 20, 1926, page 489 (*S*516).

4. A Transformer Method for Measuring High Alternating Voltages and Its Comparison With an Absolute Electrometer, **F. B. Silsbee, F. M. Defandorf.** *Journal of Research* of the National Bureau of Standards, volume 20, March 1938, page 317 (*RP*1079).

H. W. Bousman: Doctor Silsbee's remarks are very helpful, and we appreciate them very much. He has asked several questions and we are glad to answer them as follows:

1. The detector is a battery-operated electronic-tube amplifier, housed complete with batteries in the small case within the bridge shown in Figure 8 of the paper. Sensitivity is such that one microvolt may be detected.

2. The chart listing the successive steps in comparing resistors is rather lengthy. It will suffice to say that it takes account of residual conductance of the dielectric supporting the switches.

3. The calibrating balance is done at about 700 volts rms. The maximum power dissipation in a resistance coil is only 11 milliwatts; therefore, the authors have not been concerned with maintaining equal power in the coils.

A Method for Correlating Duty-Cycle Tests on Solenoids

Discussion of paper 43-108 by J. E. Ryan, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, September section, pages 579–81.

L. T. Rader (General Electric Company, Schenectady, N. Y.): A-c solenoids perform many more useful functions in industry than are generally realized. They are used primarily for applications where a high force must be exerted through a one- or two-inch stroke in a very short time, and such applications are very numerous. The problem of calculating the permissible duty cycle for such a device is practically impossible because of the large number of variables involved. Some of these will be discussed briefly.

An a-c solenoid is normally designed to give a certain pull at a certain stroke. The allowable rating is determined from a heat run in the closed position. The total watts in the device are divided among the coil, pole shader, and iron. The rating, however, also depends upon the type of mounting: for example, whether it be on a wood base or a steel base. Although the stated pull of a solenoid is given as a definite figure, that figure is usually the minimum point on a pull curve, such as shown by Figure 1 in the paper. The pull, therefore, may vary considerably from point to point, making an analytical expression for it impossible.

When a solenoid in the open position is energized, a current flows, the magnitude of which may be as much as ten times the magnetizing current of the device; in addition, there will be a transient depending upon the point of closure. In the open position, the watts are divided primarily between coil and iron, but the percentage varies with the stroke. The loss in the pole shader is negligible when the solenoid is in other than the seated position.

Mr. Ryan's analysis, depending, as it does, on only a few test runs, is extremely useful and successfully eliminates inaccuracies which might occur simply by extrapolating test data, as well as by eliminating the necessity for taking so much data. This method of analysis can also be used to compare two solenoids which have the same rated pull but are radically different in physical dimensions. By taking a small amount of data on each, the duty-cycle ability of the two solenoids can be evaluated and an accurate comparison made. This latter point is often overlooked by those who compare solenoids on a basis of rated pull and stroke and use size as the only criterion.

Mr. Ryan's method of analysis, of course, is a perfectly general one which can be applied to any electromagnetic device operated under conditions which make the calculation of performance difficult, and for which usually large numbers of experimental data must be taken.

A very interesting conclusion may be drawn from Figure 5, curve *B*. If the stroke is reduced from 100 per cent to 50 per cent, the degrees rise per operation per second falls from 155 to 45, a ratio of 3.4 to one. The solenoid, therefore, is often capable of doing more useful work when operating at a short stroke than at a long stroke. When there is a lever arrangement to the load, as there so often is, this type of data allows the optimum stroke to be chosen for maximum output. This same conclusion agrees with those made by D. K. Frost.[1]

Reference

1. Solenoids Speed Sequence Control, **D. K. Frost.** *Machine Design*, volume 12, September 1940, pages 41–4, 92.

Adequate Electrical Maintenance Essential to Transportation

Discussion of paper 43-78 by W. J. Clardy, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 517–21.

E. E. Kearns (General Electric Company, Erie, Pa.): Present-day maintenance emphasis is concentrated on practical scheduling for a particular equipment and a particular service. Greater care is given to attain a practical median between excessive tinkering on the one hand, and, as they fail, repairs on the other, and to schedule this median program insofar as possible into three gradations of inspection and a major overhaul, usually on a mileage basis.

Close engineering study is given particular equipment items so as to schedule each adjustment, renewal, or rehabilitation at its proper time in the graded programs.

For rotating generators and motors, the four most important items of maintenance can be classed as follows:

1. Keep equipment clean and as free as possible from dust, dirt, and grease. This includes non-visible locations, particularly armature core ducts. Life of modern rotating machines depends on their thorough ventilation; consequently, it is of utmost importance that armature core ducts be kept open.

2. Watch commutators closely, and dress at first signs of any distress, until commutators take on their proper polish. If eccentricity or high or low bars develop, commutators should be stoned in their bearings or turned, as close limits and concentricity are prerequisites to good commutator performance, especially on modern high-speed machines.

3. Lubricate properly and in this connection *do not overgrease antifriction bearings.*

4. Dip and bake armatures and fields at regular intervals.

The four most important items of control-equipment maintenance can be classed as follows:

1. Keep the equipment clean, and free from dust, dirt, and grease.

2. Keep apparatus properly adjusted, including contacts and contact pressures.

3. Maintain tight connections of terminals, contacts, tips, and so forth.

4. Inspect and replace items which would court trouble before next inspection.

Application of Carrier to Power Lines

Discussion and author's closure of paper 43-80 by F. M. Rives, presented at the national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, pages 835–44.

E. H. Bancker (General Electric Company, Schenectady, N. Y.): When carrier started as a youngster some 20 years ago, it undertook the hardest job first, that is, the transmission of intelligible speech. This had both good and bad consequences. It was good because it showed the need for further development work to surmount the difficulties encountered and instigated the program of development which has been continuous. It was good because it commenced the acquisition of experience with about all of the application obstacles instead of adding them one by one, as more complex applications were attempted. This has given a great many more years' experience from which to analyze and predict the probable behavior of proposed applications. On the bad side is the fact that failure to secure wholly successful operation implanted in the minds of certain individuals a distrust of anything bearing the name "carrier," and this has been most difficult to eradicate. This probably is the major factor in the relatively slow growth of carrier installations over as long a period as 20 years.

Carrier has now come of age, being something over 21 years old, and its possibilities are just beginning to be realized by those who can gain the most from its use. During these years it went through the usual development era of a new product as experience and development in associated equipment showed the way toward improvement. Like most products, this development era explored many possibilities that showed promise and eventually settled down with component designs, utilizing those constructions which give the most for the money. While carrier equipment scarcely falls in the category of mass-produced apparatus, the designs have become standardized sufficiently so that it is repetitively produced. As always, this means still more for the money since engineering can be concentrated on improving a small number of standard items rather than scattered over custom design of a wide variety of items. There is also the usual factory saving in the skill acquired by workmen and the possibility of using tools and production methods that either cut the costs of a given article or permit a better article to be made at the same cost. It is evident from this paper that many of the components of carrier have reached this standardized stage, in which the user gets an ever better product at a constant or decreasing price.

Once a product has become standardized and has been in service long enough to acquire a background of experience, it becomes possible to analyze and predict with fair accuracy its application in service. This leads toward standardized applications where the application engineer is provided with necessary information to analyze the behavior and properly apply equipment. Wide dissemination of the application data acquired by experience is necessary for the lowest-cost successful application. Users must know what the capabilities and limitations of the entire channel are, so that they may make successful applications and understand what lies behind the manufacturer's recommendation. Users sometimes intimate that they think manufacturers are loading up a proposition with unnecessary equipment just to sell more, but this is seldom true. Information such as that contained in Mr. Rives' paper should form a fundamental basis for an intelligent approach by any user to the investigation of a proposed carrier installation. It ought to be in the file of every one who has any contact with carrier.

E. E. George (Southwest Power Pool, Little Rock, Ark.): This paper presents excellent material long needed by power system operating and design engineers. It takes much of the mystery out of carrier-current application and it should result in more extensive use of carrier-current equipment as well as more economical use of the various types and sizes available.

It is hoped that other manufacturers as well as users will contribute to the discussion of this paper. It fills a gap in present power-engineering literature and deserves wide distribution and study.

J. D. Booth (Westinghouse Electric and Manufacturing Company, Baltimore, Md.): The data in this paper agree very closely with similar data collected independently by the Westinghouse Electric and Manufacturing Company. However, in a paper "Power-Line Carrier Communication" by the writer and A. P. Bock, presented in 1941 (AIEE miscellaneous paper 41-63), an average figure of four decibels was given for coupling losses including the coaxial cable. This average figure also included losses due to unavoidable impedance mismatches and stray coupling to power conductors not a part of the carrier circuit. Since these effects are usually associated with the terminal and coupling equipment they were also included by me as coupling losses rather than by a multiplying factor as shown by Mr. Rives in Table VI. While the recommended figure of four decibels is somewhat pessimistic, Mr. Rives gives a figure of one decibel which may be misleading since it does not include several important factors.

A. H. Burkhalter and **D. M. MacGregor** (Ebasco Services, Inc., New York, N. Y.): The growth of system interconnections has increased the importance of both frequency selection and carrier attenuation beyond anything suggested by previous experience with inherently isolated channels. Many of the interconnection channels must operate on a broadcast basis, and the approach to the problems of application are totally different from those encountered in the past. One such application problem with which we have been connected involves frequency allocations for some 35 new channels in an area in which a number of carrier channels are already in operation. Using the data given in Table IV of Mr. Rives' paper it has been calculated that many of these new channels will be subject to greater attenuation due to branches and loops than that caused by straight away line losses.

In discussing the reflection losses due to relatively short taps or loops, Mr. Rives omits mention of the fact that it is relatively simple to calculate the critical lengths of any given frequency. One of the smaller manufacturers dealing extensively in carrier-current equipment has prepared this information in the form of curves showing critical lengths as a function of frequency. These curves have been most useful in determining appropriate frequencies in advance, and also as an aid in locating troublesome taps or loops when high attenuations are encountered in the field.

Advance field determinations of carrier attenuation may also be used to advantage although it is sometimes found that variations in switching setups make it appear that there is no suitable frequency. This is particularly true when a number of channels are to operate in a single closely spaced frequency group from several remote points to one common receiving point. Probably the results of such an attenuation study are chiefly useful in avoiding particularly poor frequencies.

The arrangement for a line-tuning assembly for three independent and widely spaced carrier channels, shown in Figure 5, can be simplified by the elimination of one low-voltage trap without increasing difficulty of adjustment and with some reduction in both losses and cost.

The use of band-pass elements for multichannel by-pass circuits, mentioned briefly in Mr. Rives' paper, should receive very serious consideration because of the flexibility, low loss, and relatively low cost of such elements compared with the more conventional by-pass assemblies. A recent problem involving a by-pass for three phase-ground channels and one phase–phase channel and including a shift of phase use has been solved very nicely by the use of a band-pass system for the phase–ground channels combined with a conventional by-pass for the phase–phase channel. Adoption of this arrangement reduced the number of capacitors and the quantity of concentric cable very materially without any increase in losses.

Mr. Rives is to be complimented, not only because his paper is clear and concise, but more particularly because it represents a long needed step toward advancing carrier-current application. Information in the paper is of immediate interest to all who have occasion to apply or operate carrier equipment, and its presentation should go far to remove from the minds of operating engineers the feeling that carrier is both mysterious and unpredictable.

S. L. Goldsborough (Westinghouse Electric and Manufacturing Company, Newark, N. J.): This paper is a compilation of information concerning the factors to be considered in the superposition of carrier-current channels on power lines. It is believed that the paper is a beneficial contribution in that it brings together in one place most of the facts which must be known by those engineers who have to deal with carrier-current channels on power lines.

I believe the part pertaining to the application of line traps is liable to give a somewhat false impression. As far as carrier relaying is concerned, the only absolutely compelling reason for using line traps is to prevent interference by external ground faults. Independence of these external faults can be attained by the use of one line trap. The other beneficial results from the use of line traps, such as, "To reduce transmission losses in irrelevant branch lines, to minimize the effect of low-impedance shunts, and to isolate effectively the carrier channel" cannot be realized to the fullest extent by the use of a line trap in one phase. The paper gives the impression that the carrier channel can be isolated effectively by the use of single-phase trapping. This is definitely not true since a number of cases have come to my attention where apparently the effect of one trap in isolating the carrier frequency from the rest of the system was practically negligible. It should be remembered that there is comparatively large coupling between the phases of the transmission line and also to a closely spaced parallel line. The application of traps to only one phase does not affect the coupling of the other two phases, and thus the carrier energy can permeate the rest of the system with the possibility of encountering severe reflections. Accordingly, the statement in the paper that with line traps the carrier can be isolated in a particular section and, therefore, its frequency can be predetermined is not true, but becomes partially correct if all three phases are trapped.

A statement is made in the paper that the main disadvantage of the line-to-ground coupling is in its somewhat higher attenuation and higher noise level as compared with the interphase circuit. I would like to ask the author if he has any definite figures regarding this increase in attenuation and noise level.

With reference to Table IV, I would like to ask if the decibel losses were calculated or were they a result of laboratory or field tests? Some of them looked quite optimistic. The loss figures in item *B* and particularly item *E* are too low. For example, item *B* with the assumption that all lines are long so that reflections are negligible, the power entering the junction goes three ways. It goes forward on two lines and backward on the incoming line. The voltage at the junction is the same on all lines, and, therefore, the power on each line is one-third the incoming power, or 4.7 decibels loss. Item *E* would be 7 decibels on the same basis. Item *E* may give very large losses if the loop differs from the line by an odd number of half wave lengths. In other words, on a loop circuit the carrier waves reaching the junction point adjacent to the receiver may be out of phase by any degree, and therefore, the loss may be practically anything.

F. M. Rives: Mr. Booth appears to think that a loss of only one decibel for the line-tuning and coupling equipment, as indicated by item *A* of Table IV, is somewhat on the optimistic side. As a matter of actual measurement, the losses in this portion of the circuit usually fall well under this figure except for the most complex tuning arrangements. The figure of one decibel, therefore, is considered to be a fairly safe average. Since the losses in the lead-in cable are directly dependent on the length and frequency used, it is frequently advantageous to compute this loss item separately. The mutliplying factor, as indicated in Table VI, is not intended so much to cover miscellaneous losses as it is to provide for variations in the transmission-line loss and to differentiate between the signal-noise requirements of different services.

In the discussion by Mr. Burkhalter and Mr. MacGregor it was indicated that in dealing with reflection losses due to short taps or loops the critical line length for any frequency could be readily calculated. While it is true that the frequency interval between nodes or peaks in the transmission characteristic may be determined fairly closely from the line length, the actual frequency or frequencies at which a tap line will appear as a very low impedance or a very high impedance depends also on the terminating impedance as well as the electrical length. Since most branch or tap lines terminate in a transformer bank, for which the carrier-frequency impedance is not readily determined, it is not usually possible to calculate the frequencies at which a particular tap will cause trouble.

In his discussion, Mr. Goldsborough indicates that a carrier-relay channel may be made independent of external faults by the use of one line trap. It is assumed that he means one line trap associated with each terminal; that is, for a two-terminal channel a line trap would be used in one phase at each of the two terminals. He is quite correct in pointing out that the other benefits provided by line traps cannot be realized to the fullest extent by single-phase trapping. However, the increase in efficiency gained by trapping all three phases of a line for a single channel is usually not sufficient to justify this additional expense and complexity. In speaking of isolating a channel by the use of traps, it is not intended to imply that the use of traps in a single phase will prevent the transmission of the channel frequency into other phases and into other parts of the system beyond the traps. Even with traps in all three phases some carrier-frequency energy will get through the traps and certainly will be transmitted around the traps where parallel circuits are present. However, single-phase trapping will isolate a circuit effectively as far as the influence of portions of the line outside of the trapped section on the carrier-frequency characteristics of the trapped section itself. This definitely provides freedom in the choice of the channel frequency as far as its transmission characteristics are concerned; it also provides independence of line switching. Both of these advantages are quite important for certain types of service.

In answer to Mr. Goldsborough's request for definite figures on the increase in attenuation and noise level encountered on phase-to-ground circuits as compared with phase-to-phase circuits it may be stated that data from actual field measurements indicate an increase in attenuation of from 10 to 50 per cent for the phase-to-ground circuit, depending on a number of factors, including ground conditions, presence of other possible return paths, and others. No definite figures on comparative noise levels are available; however, observations on many installations have indicated a general increase in noise level for the phase-to-ground which has been estimated to be in the range of from one to six decibels.

The circuit configuration losses indicated in Table IV are largely empirical, based on accumulated experience. However, they are not too far from calculated values for similar conditions.

Carrier-Current Differential Protection for Transformer Banks

Discussion and author's closure of paper 43-91 by T. A. Cramer, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 545–8.

W. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): Probably the most worth-while contributions by relay engineers to the supplying of electric power are those which make possible the rendering of adequate service with less system investment in major equipment. Particularly in wartime, when materials are precious, we are justified in expanding the range of special schemes for this purpose. Therefore, the proposed method of utilizing a carrier-current channel to trip a remote oil-circuit breaker when the local differential relays indicate a transformer fault is most timely.

Widely different degrees of reliability of a pilot-wire or carrier-current channel may be required, depending on the method of relay protection used. A-c pilot-wire schemes which inherently may experience false operations if the pilot-wire circuit is momentarily short-circuited or open-circuited may be used on short privately owned pilot-wire channels with excellent reliability.[1] When leased circuits of greater exposure (and which are exposed to personnel interferences at exchanges) are used, it is the more usual practice to provide relay schemes such as the d-c directional comparison type (or a-c pilot-wire systems interlocked with basic fault-detector elements of the d-c type) so that the relay scheme is immune to pilot-wire disturbances which occur when there is no fault on the protected power line. Transferred tripping has been used satisfactorily with d-c pilot-wire schemes, particularly if the impulse is maintained at a level above the usual telephone-exchange battery circuits.

Carrier-current protection is inherently of the type on which a momentary disturbance to the channel does not cause false operations in the presence of load current. Accordingly, some trepidation is experienced in viewing the extension of carrier-current channels to the transfer-tripping field, which by its nature cannot be interlocked with fault detectors. The fact that the author indicates an allowable maximum time-delay margin of 30 cycles shows that he perhaps shares these misgivings.

Satisfactory experience is reported with a three-tone (audio) carrier-current transferred-tripping scheme.[2] However, in some cases a local high-voltage grounding switch has been employed to be operated by differential relays rather than to use carrier-current transferred tripping, although the channel was available for relay protection.[3]

I would like to ask the following questions:

1. In the author's opinion, can a three-tone (audio) system such as described in January of 1942[2] with no intentional time delay, provide approximately the same reliability as the single-tone system with a 30-cycle delay?

2. Since lightning surges or their effects are ordinarily thought of as lasting less than one cycle, it would be interesting to know what specific reasons led to the adoption of the 30-cycle maximum time.

3. Can the scheme described be readily adapted to be used with a conventional two-frequency carrier telephone system?

References

1. Pilot-Wire Relaying on a Metropolitan System, **T. G. LeClair, E. L. Michelson.** AIEE Transactions, volume 62, 1943, August section, pages 511–15.

2. Multichannel Carrier-Current Facilities for a Power Line, **P. N. Sandstrom, G. E. Foster.** AIEE Transactions, volume 61, 1942, February section, pages 71–6.

3. Performance Requirements for Relays on Unusually Long Transmission Lines, **F. C. Poage, C. A. Streifus, D. A. MacGregor, E. E. George.** AIEE Transactions, volume 62, 1943, June section, pages 275–83.

E. H. Bancker (General Electric Company Schenectady, N. Y.): Mr. Cramer's paper should be interesting to those who are faced with extensions to their system under both the war restrictions as to the equipment which may be purchased, and under the economic restrictions of making the extensions at the least-possible cost compatible with successful operation. There have been many lines constructed in the past few years that terminate in transformers without high-voltage breakers, and there will undoubtedly be more. This type of installation always raises the problem of how to give adequate protection against damage within the transformer from a fault within the transformer. Differential relays may readily be applied between the low-voltage current transformers and bushing-type current transformers in the transformer bushings, but tripping of the low-voltage breaker by such relays does not remove the fault. Unless the fault is of sufficient severity to be recognized by the protective relays at the far end of the transmission line, it will continue to wreak destruction within the transformer until it does become bad enough to operate the line relays at the far end. This may result in a very expensive repair.

As an alternative to the high-voltage breaker, a carrier channel using much less critical material and at lower cost, perhaps even available for other relay purposes, can extend the tripping circuit of the differential relays to include the breaker or breakers at the other ends of the transmission line. If carrier is already used for the line-protective relays, a relatively small amount of additional equipment will permit its use as an extension of the trip circuits of the differential relays.

Again, the results of experience enter the picture and after experimenting with the several possibilities that Mr. Cramer points out, a standardized method of sending a tripping signal has been developed. It should be emphasized that this is a much more difficult task than sending the blocking signal used in the customary carrier-pilot relaying, because a transient block generated by lightning, switching, or other causes does no particular harm, but transient tripping signal cannot be tolerated since it results in an unnecessary trip that should not be followed with a reclosure until the cause for the trip has been investigated and determined as external to the transformer.

Ben C. Hicks (Shawinigan Water and Power Company, Montreal, Can.): In his excellent paper, Mr. Cramer refers to the scheme he is describing as a remote-tripping scheme. The Shawinigan Water and Power Company has, on one of its 220-kv lines, a similar scheme which it refers to as a transfer-trip scheme. This scheme operates on the heterodyne principle which Mr. Cramer has included as number 3 in his list of possible methods of conveying remote-tripping information over a carrier-current channel. Experience with this installation has confirmed amply the need of special care being taken to avoid faulty operation due to various kinds of interference. It may be of interest to mention one instance of such faulty operation that was experienced.

In the case referred to, all three ends of a three-ended 220-kv transmission line were terminated in transformer banks. A fault occurred on the low-voltage side at one terminal, outside of the zone of the transformer-differential protection and, contrary to expectation, caused the operation of the transfer-trip feature. This fault was studied by the manufacturer of the carrier-current equipment and it was found that a prominent 2,000-cycle frequency was present which passed the 1,000-cycle high-pass filter provided as part of the heterodyne receiver. A slight modification was subsequently made in the heterodyne receiver to prevent a recurrence of the faulty action from this cause.

There are two questions I would like Mr. Cramer to comment on. The foregoing incident suggests the first one. In the scheme described by Mr. Cramer a 3,000-cycle, audio tone is utilized to convey the tripping information. Is it not possible that faulty operation would result should a system fault generate a strong 3,000-cycle frequency? The second question concerns the hand-reset multicontact auxiliary relay. It would appear to be an advantage if this relay did not require the attention of the operating staff to reset it. Would it be possible, with minor modifications, to use a self-resetting relay, or is it essential that a hand-reset relay be used?

T. A. Cramer: In reply to Mr. Hicks' discussion, the possible occurrence of a strong 3,000-cycle voltage on the high-voltage line has been considered in the design of this equipment. By using two inductively coupled tuned circuits in the receiver input, the sensitivity of the receiver at 3,000 cycles is very low. The receiver input circuit is tuned to the carrier frequency, and frequencies in the pass band of these circuits are accepted by the receiver. When the incoming carrier signal is modulated at 3,000 cycles, the output from the receiver operates the relay tuned to this frequency. This double selection gives ample selectivity to prevent operation by switching surges and faults.

In reply to Mr. Hicks' second question, it is possible to use a self-resetting auxiliary relay, but there is some advantage in using a relay which requires the recognition of this operation by the station attendant. When a remote transformer fault is tripped by the carrier equipment the operator should not reclose the line to the faulted transformer, and the hand-reset tripping relay warns him of this fact. If automatic reclosing is used, the hand-reset relay is used to block the recloser until the transformer fault is cleared from the circuit.

The discussion on this paper by Mr. Brownlee brings out some points not covered in the paper. Regarding his first question, I believe that the three-audio-tone system would have approximately the same reliability as the single-audio-tone system with 30 cycles delay.

In reply to his second question regarding the reasons for using a 30-cycle maximum time, this is determined largely by the characteristics of the time-delay auxiliary relay itself. A time-delay relay of the copper-slug type was selected and could be adjusted over a range of 6 to 30 cycles. Field tests have been made on various installations of this type of equipment, and the relay current in the 3,000-cycle relay was observed during staged short circuits, energizing and de-energizing transmission-line sections and transformer banks, and in no case was any interference observed. Until further operating experience has been obtained it has been the practice to adjust these auxiliary relays for an operating time of approximately 15 cycles.

The conventional two-frequency carrier telephone system lends itself readily to the addition of this type of remote tripping. A cut-off filter would be used in the telephone circuit to limit the speech band to 2,500 or 3,000 cycles, and the tripping tone would be sent at some higher frequency such as 3,500 or 4,000 cycles.

It is hoped that a paper will be presented at some later date, giving information on the operating experience obtained by various users of this type of equipment.

A Simple Method for the Determination of Bushing-Current-Transformer Characteristics

Discussion and author's closure of paper 43-110 by S. D. Moreton, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, September section, pages 581–5.

C. A. Woods, Jr. (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): It is indeed encouraging to find additional support for the use of open-circuit excitation data in the application of bushing-type current transformers in relaying service, as proposed by Mr. Bottonari and the writer in 1940.[1] At that time we proposed the use of scalar addition of excitation and burden currents to obtain the total primary current, as evidenced by part *b* of Appendix III and Figure 8 of that paper. Its justification was based upon the fact that the burden power-factor angles and excitation current angles were usually of the same order of magnitude when the transformer core was not saturated. When the core was saturated, the excitation current increased so rapidly with any increase in primary current that the error introduced by this method was negligible. Mr. Moreton's paper provides further justification for the scalar method by showing that the errors introduced by the scalar method, as compared with the vector method, are less than the probable difference in the performance between commercial units of the same design.

There has been considerable discussion of the best method of calculating ratio- and phase-angle curves from the excitation data. This has been very natural since such calculations provided the best means of comparing results of transformer performance determined from excitation data with that obtained from ratio- and phase-angle tests. However, as the real meanings and uses of the excitation curves become more widely known, there should be a diminishing need for calculating ratio- and phase-angle curves.

The graphical methods of curve drawing and chart construction, as covered by this paper, are of particular interest to transformer designers and those who must prepare curves of transformer performance. However, the relay engineer will be concerned primarily with the importance of the ratio of *Ie/Is*, as covered by equations 5 and 6, and Figure 5 of the paper. This term, *Ie/Is*, when multiplied by 100 has been defined previously[2] as "per cent ratio error," and the equation for the curves of Figure 5 has been published previously.[3] Thus, both the ratio correction factor and phase angle of the transformer are a function of the term *Ie/Is*, which can be determined readily from the excitation data.

From the standpoint of simplicity and economy, it would seem to be to the advantage of the relay engineer to have the maximum of transformer data in as compact a manner as possible, requiring a minimum of calculating equations and charts. Based on the apparently justifiable assumption that the majority of relay applications are made with current transformers operating within a range of *Ie/Is*, equal to or less than 0.1, there should be little need for supplying any data, other than an excitation curve of secondary volts versus secondary-excitation amperes similar to the curves of Figure 4. Under these conditions, the per cent ratio error is less than ten per cent, and, as shown by Figure 5, the phase angle is less than six degrees, irrespective of the burden power-factor- and excitation angles. In a few problems, more detailed calculations might require the use of the excitation angle as shown in Figure 6. It is obvious that the curves of Figure 6 give all the data that are required, as the curves of Figure 4 can be calculated readily from Figure 6. However, since those of Figure 4 do not require conversion to the turns base of the current transformer, they provide a means of checking the majority of applications with a minimum of effort.

Since ratio correction factor and phase-angle curves are often inadequate or awkward in the investigation of certain relaying problems, such as those involving transient performance, parallel operation, current leakage paths through other transformer secondary windings in ground relay circuits, and similar problems, it would appear that only the excitation data need be furnished for bushing-type current transformers. It should be provided in such a manner that the type and method of calculation could be any one of the many proposed, which is best suited to the specific problem at hand. The curves of the form shown in Figure 4 and those in Figure 6, with the magnetizing and loss components added, will fulfill adequately the foregoing requirements. This would result in a material reduction in time spent on making ratio and phase-angle curves, which are often inadequate in range of current or burden, and the relay engineer would not have his files burdened with a large volume of unnecessary data.

If a method of determining secondary leakage reactance of current transformers, such as proposed by Mr. Wentz,[4] comes into general use, then the need for overcurrent ratio and phase-angle curves is entirely removed, and the methods used to portray the overcurrent performance of bushing current transformers could become equally applicable to all types of current transformers.

References

1. Overcurrent Performance of Bushing-Type Current Transformers, **C. A. Woods, Jr., S. A. Bottonari.** AIEE Transactions, volume 59, 1940, September section, pages 554–60.

2. American Standard *C*57.1. Transformers, Regulators, and Reactors. Paragraph 4.032 (a) (1).

3. Current- and Potential-Transformer Standardization, **J. E. Clem, J. H. Neher, E. L. Harder.** AIEE Transactions, volume 61, 1942, September section, pages 698–706.

4. A Simple Method for Determination of Ratio Error and Phase Angle in Current Transformers, **E. C. Wentz.** AIEE Transactions, volume 60, 1941, October section, pages 949–54.

A. C. Schwager (Pacific Electric Manufacturing Corporation, San Francisco, Calif.) Previously known methods for the calculation of the ratio error of current transformers were based upon vector addition of the secondary and the exciting currents. In his paper, the author shows that, for practical purposes, inphase addition produces sufficiently accurate results.

The author is to be congratulated for this discovery and for having postulated so clearly its usefulness, the result of which appears to be the simplest ratio-error calculation method proposed to this date. Although transformers today are using steel having exciting current phase angles as shown in Figure 6 (25 degrees at maximum permeability), it must be realized that the method becomes inaccurate when lower-power-factor steel is used. An explanation of this limitation seems indicated in order to prevent misapplications of the method, possibly on old transformers in service.

On page 583[1] the author states: "When the ratio errors are desired for a range of primary current values of a multiratio transformer, the labor of calculation becomes tremendous, even with the simplification of inphase addition." He then develops an interesting graphical method and proposes the use of a template.

The writer assumes that, as complete information on a particular transformer, the author intends to furnish a chart similar to Figure 1 or Figure 12 of his paper for a two-ohm burden and supplement this chart by an exciting current chart and a template for use when user's burden differs from two ohms, which is likely to be the case in 99 per cent of all applications. In Figure 4 of the closure to the discussion on a paper on current transformers[2] the writer has shown, that by using a slightly different scale, the author's Figure 1 can be made to apply to any burden value. Combined with the author's findings, this chart seems to offer a complete solution to the ratio-error-data problem, since from it the ratio error, or RCF, can be read directly for:

1. Any primary current value.
2. Any burden value.
3. Any practical burden power factor.
4. Any secondary turn number.

Such a chart eliminates a good portion of the calculations necessary in the author's method and avoids the necessity of the use of a template. It is suggested that it be given further study, particularly on the part of the relay engineer who is vitally interested in obtaining performance data as directly as possible.

References

1. A Simple Method for the Determination of Bushing-Current-Transformer Characteristics, **S. D. Moreton.** AIEE Transactions, volume 62, 1943, September section, pages 581–5.

2. Current-Transformer Performance Based on Admittance-Vector Locus, **A. C. Schwager.** AIEE Transactions, volume 61, 1942, pages 467–9.

W. R. Brownlee (Commonwealth and Southern Corporation, Jackson, Mich.): Many simple methods have been proposed within the last year or two for the determination of bushing-current-transformer characteristics, but most of these have recommended taking into account both the inphase and quadrature components of magnetizing current and relay impedance. While the vector calculations involved are, in themselves, quite simple, the volume of work required, even with such useful tools as a vector slide rule, is generally all out of proportion to the value of the accuracy thus obtained. This is particularly true in view of the uncontrollable variations in the iron characteristics and in saturation of the relay burdens described by the author. It is, therefore, believed that he has done a distinct service in demonstrating so clearly, that for the situations most commonly encountered, the direct arithmetical addition of bushing-current-transformer exciting current to the relay current to give the equivalent primary current (and thus, the ratio) will involve errors within usual slide-rule accuracy. The curves, which illustrate at a glance the approximate error to be expected by this method under various conditions, provide an excellent timesaver.

Unfortunately, saturation curves are seldom available for any except the newer bushing current transformers. Use has been made of the inphase method to secure synthetic saturation curves from available ratio curves. These in turn are then used to secure ratio curves for burdens higher than those covered by the manufacturer's information (particularly for ground relays), for ratios on taps for which curves are not available, or to extend the range of certain curves. For assumed values of primary current, the secondary currents are calculated by means of available ratio curves, and the difference between these currents and values secured by dividing primary currents by the turn ratio is plotted as the difference, or exciting current. Corresponding voltage values are obtained by multiplying the secondary-current values by the assumed burden impedance.

Accurate determination of ratio is frequently required for inverse characteristic ground relays, since the burden is usually high, and since magnetizing current must be supplied to three current transformers instead of to one. Even when using the inphase method, the calculation of residual-current-transformer performance may become involved. If all of the current is considered to flow through one conductor, then the voltage across one current transformer will be that caused by the impedance of the ground-relay plus the phase-relay burden, whereas the voltage across the idle current transformers will be the result of the impedance of the ground relay alone. Separate magnetizing currents may be computed and added to give the over-all equivalent ratio. A simpler method, which is entirely adequate unless high saturation is involved, is to consider that the entire circuit current is of zero sequence, so that currents equal and in phase occur in all three conductors. In this case, the same voltage is impressed across all three current transformers and is equal to the total or ground current multiplied by the ground-relay burden plus one third of the ground current multiplied by the phase-relay burden. Since actual ground faults will involve conditions between these two, the method recommended is to add one third of the phase burden to the ground burden and use this impedance to secure magnetizing-current values from the saturation curve, multiplying such values by three before adding them to the ground-relay current values to secure over-all ratio.

A "super-short-cut" method of securing residual ratio is to read from standard ratio curves the value corresponding to the primary current (total ground current) with a burden equal to the ground-relay impedance plus one third of the phase-relay impedance and multiply the indicated turn-ratio difference by three. For example, if the standard curve indicates a ratio of 62:1, whereas the turn ratio is 60:1; then the proper residual ratio is 66:1.

Theodore Specht (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): Mr. Moreton's method is quite ingenuous, but the making of the template would be no small task, one of which would be required for each size core. Also, the customer still would not be able to determine performance for any burden he desired.

Another approach to this problem can be made by a method that has two advantages. With one curve the customer can determine the ratio of a multiratio bushing-type current transformer for any burden or ratio, and it is a simple curve for the manufacturer to make. The curve is a modified excitation curve, not for the secondary winding of the transformer as in the usual case, but for the primary winding. In magnitude, the primary current is

$$I_p=I_sN_s+I_{ex}\cos(\alpha-\phi)$$

where

I_sN_s is secondary ampere turns.

I_{ex} is the core exciting current from the primary of the transformer.

ϕ is the burden phase angle.

α is the core-exciting-current phase angle.

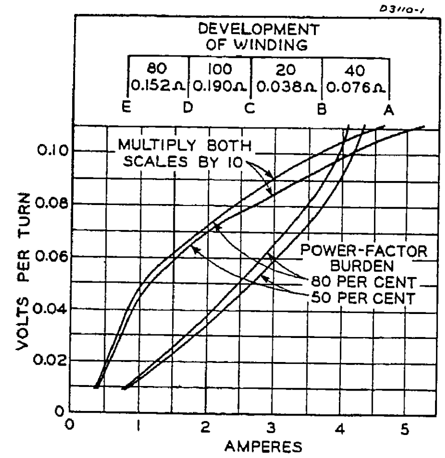


Figure 1. Curve for particular transformer instructions for using curve:

Instructions for using curve:

Given:

Secondary amperes
Secondary burden, ohms

Procedure:

1. Find total burden; add winding resistance to actual burden

2. Find volts per turn from

$$V/T=\frac{\text{secondary amperes}\times\text{ohms}}{\text{turns in winding}}$$

3. Read "amperes" from curve and add to this the secondary ampere turns to secure true primary current

Figure 1 is a curve of primary voltage as a function of $I_{ex}\cos(\alpha-\phi)$, or amperes, as it is called, for two values of burden power factor. To use the curve, the secondary voltage is calculated and divided by the secondary turns to secure the primary voltage. The value of amperes is read from the curve and added to the secondary ampere turns to secure the magnitude of the primary current.

From the manufacturer's standpoint, these curves are simple to make. For a particular grade of steel, a master curve, Figure 2 of this discussion, was drawn for a transformer with a net core area of $2^1/_2$ square inches and a mean core diameter of ten inches. These core dimensions, with the current scale one ampere per inch and the voltage scale 0.01 volt per inch, then gave a curve that fit the sheet well. On the master curve, though, no scales are actually drawn in.

For any particular transformer the voltage will be proportional to the core area. Hence, at one inch on the voltage scale on the master curve

$$E_p=(0.01)\left(\frac{A}{2.5}\right)$$

and, if at y inches the voltage is 0.01 volt

$$\frac{0.01\left(\frac{A}{2.5}\right)}{1}=\frac{0.01}{y};\qquad y=\frac{2.5}{A}\text{ inches}$$

A curve for a particular transformer is prepared by drawing in the voltage scale on a reproduction (photolitho) of the master curve with 0.01 volt at $2.5/A$ inches, where A is the cross-sectional area of the core.

By a similar reasoning, the exciting current is proportional to the mean diameter of the core. Hence, at one inch on the current scale of the master curve

$$\text{Amperes}=1\frac{ID+OD}{(2)(10)}$$

and, if at x inches, the amperes are 1 ampere

$$\frac{1\frac{ID+OD}{20}}{1}=\frac{1}{x};\qquad x=\frac{20}{ID+OD}$$

Therefore, on the reproduction of the master curve for a particular transformer, the current scale is drawn in with 1 ampere at $20/(ID+OD)$ inches from the origin.

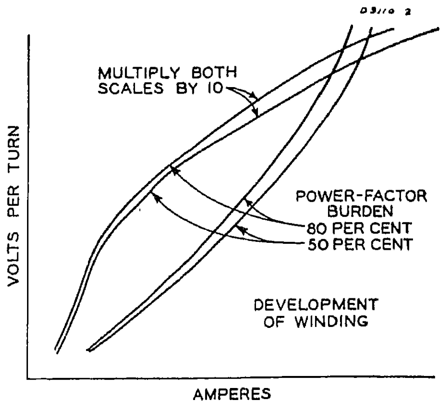


Figure 2. Master curve, reproductions of which are used by manufacturer in preparing curves for particular design

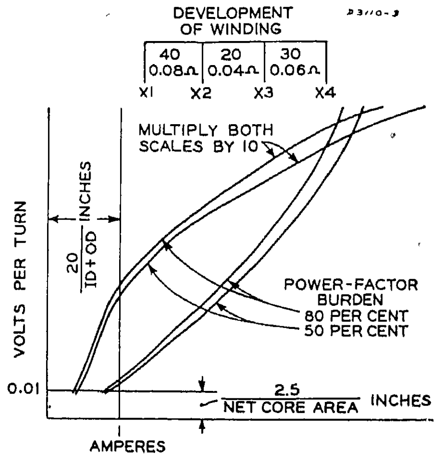


Figure 3. Instructions for manufacturer's engineers

A reproduction of the master curve is ordered, a scale put on according to above formulas, and a curve number assigned to the reproduction

These instructions are given in Figure 3 of this discussion, which shows where to draw in the one-ampere line and the 0.01-volt line. Figure 1 is a curve drawn in this manner for a core with an inside diameter of six inches, an outside diameter of $7^1/_2$ inches, and a net core area of 4.28 square inches. On the curve is shown a development of winding and the winding resistance, along with instructions on how to use it.

Another possible use of these curves is to predict the inphase component of current drawn by "dead" current transformers in various relaying schemes. This current can be referred to the secondary by dividing the amperes from the curve by the secondary turns.

This system of making curves then has the advantages of simplicity in making, and greater flexibility and range in use. A similar scheme can be used to determine phase angle.

W. E. Marter (Duquesne Light Company, Pittsburgh, Pa.): Mr. Moreton's paper is a valuable contribution to the information on current-transformer characteristics, and, because of the simplification introduced, the methods described in the paper should be accepted widely by operating engineers.

Since the method depends on data taken from excitation curves, it is thought that some additional comment by the author on the method of obtaining the excitation characteristic would be helpful, and that possibly a standard method of obtaining excitation characteristics should be considered.

It has been found in making test-excitation curves on the secondary winding of a bushing transformer that considerable variation in the curve, above the knee of the excitation curve, is obtained, depending on whether sine-wave current or sine-wave voltage is maintained by the test source, and additional variation exists, depending on the type of instruments used to measure the current and voltage.

Mr. Moreton, in the summary of his paper, has pointed out the necessity for more complete information on relay burdens at overcurrents. It was suggested to the relay subcommittee, when revision of the manufacturer's catalogue sections was being discussed, that a curve be included in catalogue data on relays showing the burden at various currents. No results of this suggestion are apparent at the present time.

S. D. Moreton: The fundamental purpose of this paper was based on the desire to offer a satisfactory and conclusive answer to the questions raised by American Standard *C* 57.1, section 4.033, "Application Data for Current Transformers" paragraph *C* and *D* which state:

> *C* "Standard Ordinates and Type of Scales for Curves. Not yet established.
>
> *D* "Standard Method of Calculations. Not yet established."

The use of log–log paper for giving excitation characteristics seems advisable in order to cover a sufficient range of flux density for all relaying applications. The use of the special log–log paper with its triscales seems to present an ideal way of giving all ratio characteristics without calculation. The results obtained by inphase addition have been proved to be within acceptable limits based on available data for relaying applications and accepted without reservation by the discussers. The advantages of inphase addition are manifold:

1. The necessary data are kept to a minimum, since inphase addition does not require a knowledge of excitation and burden phase angles.
2. The required data may be measured easily with available instruments.
3. The calculated results give values on the pessimistic side, thus automatically compensating for the phase-angle characteristics of the transformer in regard to the relay application.
4. The calculation procedure is straightforward and thus easily remembered by the infrequent user.
5. Problems involving ground relaying with many transformers effectively in parallel are easily handled.

Mr. Schwager questions the accuracy of this method when used to calculate the characteristics of transformers having low-power-factor steel. Figure 4 of this closure shows the excitation characteristics of such a low-power-factor steel having a minimum phase angle of 50 degrees and is based on the same size core as the characteristics given in Figure 6 of the paper, which represents a high-power-factor steel having a minimum phase angle of 25 degrees. The errors introduced by inphase addition for this low-power-factor steel are best shown graphically, as in Figure 5 of this closure, which is comparable with Figure 1 of the original paper. As Mr. Schwager pointed out, the errors are somewhat greater for a high-power-factor burden, but they are also correspondingly smaller for a low-power-factor burden. If these calculated results are compared with Figure 6 of this closure, which represents the band ratio characteristics based on ±50 per cent variation in exciting current, the errors introduced by inphase addition are found to be a relatively small part of the total possible variation. A band curve of ±50 per cent is chosen as typical of the variation obtained on this old-type hot-rolled steel, as mentioned in the original paper under "Accuracy Factors." Mr. Schwager also mentions that by the use of Figure 4 of the closure to the discussion of his paper a good part of the calculations necessary for the template method could be avoided. Mr. Schwager's calculations give points on the ratio-correction-factor curves, many of which are necessary to determine the curve. The major advantage of the template method is that by solving only two equations: Per cent $I_s = CN/Z$ and $RCF - 1 = D/N$ (per cent I_s) which is only a single point, the complete ratio characteristic is obtained, and thus Mr. Schwager's point-by-point method is eliminated.

Mr. Marter mentions that in making tests on bushing current transformers considerable variation in the curve above the knee is obtained, depending upon the source and the type of instruments used (see Figure 7 of this closure). This variation is, of course, brought about by the presence of harmonics caused by the saturable characteristic of the iron core. If a sine-wave voltage is used any type voltmeter will give the same results, since the harmonics then must appear in the current circuit; but, because of the wave shape and the flatness of the curve above the knee, there is very little change in the current. If sine-wave current is used an iron-vane-type voltmeter will indicate an rms value of the wave that is appreciably greater than the results obtained with a rectifier-type voltmeter, which indicates the fundamental component very closely on this type of peaked wave. The majority of the data available, including the curves in this paper, are based on fundamental magnitude only, whether they are measured with a sine-wave voltage and any voltmeter, or a sine-wave current and a rectifier-type voltmeter. Thus, this type of excitation curve shows very clearly that there is relatively little increase in the fundamental component of the secondary current from a bushing current transformer once the knee of the curve has been passed. The core can be considered as a generator of odd-harmonic voltages for current in excess of that which saturates the core, and thus harmonics are fed into a parallel path consisting of the primary line circuit and the secondary burden circuit. Usually the impedance of the primary line circuit is relatively high compared with the generated back voltage, and the harmonics tend to flow entirely in the secondary burden circuit, distorting both the secondary voltage and the secondary current. These harmonics affect the relay response in different fashions, depending upon their sensitivity to the harmonics. Fortunately relay burdens, being primarily inductive, have burdens proportional to the order of the harmonic, and, even though the harmonic voltage is high, the actual current flowing through the relay is proportionately small. Finally, once the knee of the curve is passed, the ratio error increases abruptly, as evidenced by the ratio-correction-factor curves on log–log paper, and thus the application engineer would avoid operation beyond this point; therefore, it makes little difference what happens once the knee of the curve has been passed, since it is of academic interest only. Thus the conclusion is reached that the excitation curve could be taken in any manner desired; it is not necessary to use either a sine-wave voltage or a sine-wave current, and the source of excitation may be supplied, either from the primary or from the secondary. But it would be advisable to use a rectifier type of voltmeter in order to avoid the misleading results obtained by the generation of harmonics.

Mr. Specht, in his first paragraph, evidently misunderstood one of the main features of the template method. The ratio-

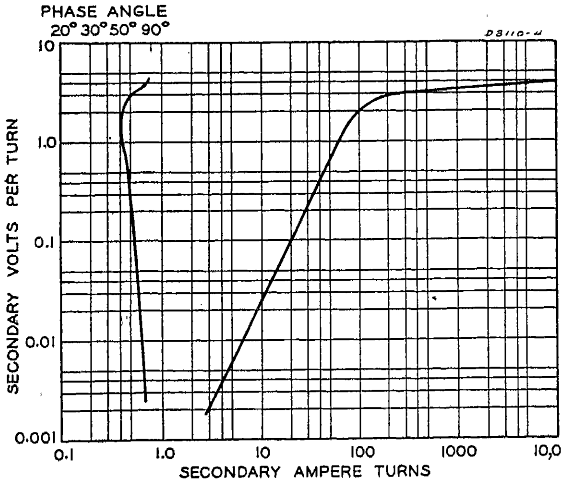


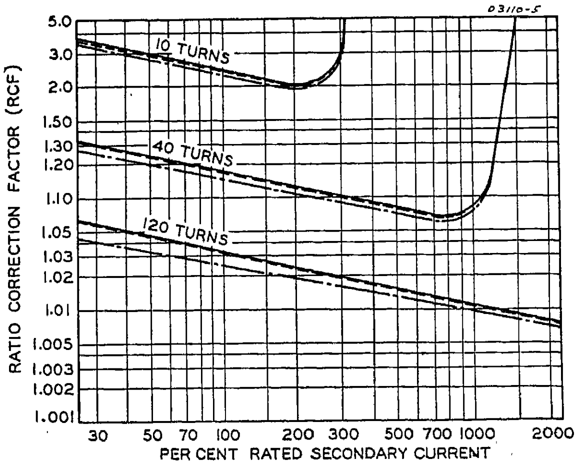
Figure 4 (left). Excitation characteristic of low-power-factor steel

Figure 5 (right). Calculated ratio characteristics with a two-ohm burden for the low power-factor steel

——— Inphase addition

—·— 90 per cent power factor

– – – 50 per cent power factor



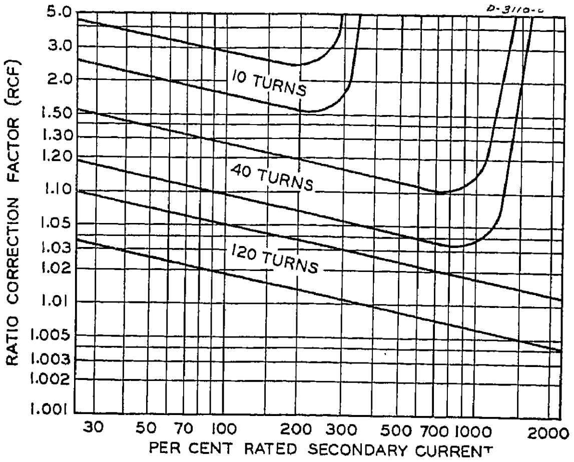


Figure 6. Band ratio characteristics on three ratios of a 600/5 multiratio transformer based on low-power-factor steel and ±50 per cent variation of excitation characteristics

correction-factor template, as given in Figure 11 of the paper, is good for all sizes of cores and all burden magnitudes. Since it is based on inphase addition, it is independent of burden and excitation phase angle. In addition, it is good for all commercial frequencies since the shape of the curves is unaffected by frequency; thus, one template will give any or all ratio characteristics.

The method of calculation described by Mr. Specht is based on the arithmetical addition of the primary component of the exciting current, which is in phase, with the secondary current to the secondary ampere turns to obtain the primary current. It is the same method suggested by Mr. Agnew.[1] This same fundamental method was also used by Mr. Wentz in his paper[2] on current transformers. Vectorially speaking, this type of addition gives $I_p \cos \beta$ which is in error to I_p by the magnitude of $\cos \beta$. Thus, the results so obtained are on the optimistic side, since they indicate ratio errors smaller than those obtained from the actual transformer. The application engineer, therefore, would have to allow a "factor of safety" to ensure correct operation of the relay scheme. This error introduces limitations on the use of the method, particularly as the difference angle $(\alpha - \phi)$ approaches 90 degrees, which is entirely possible with a unity-power-factor burden and core excitation just a little above the knee of the curve. Here the expression $I_{ex} \cos (\alpha - \phi)$ approaches zero as $(\alpha - \phi)$ approaches 90 degrees, thus making I_p equal to I_sN_s. Even though this is an extreme example, it typifies the trend of $\cos (\alpha - \phi)$, thus hiding the fact that the transformer is very badly off ratio. Conversely, the inphase addition method gives values on the pessimistic side, since it indicates ratio errors in excess of those obtained from the actual transformer, and thus a "factor of safety" is not necessary.

Mr. Specht's explanation of the master curves could be summed up more easily by stating that the voltage scale is proportional to the cross-sectional area of the core, and the current scale is inversely proportional to the mean length of magnetic path. Manufacturers could supply easily both an excitation curve based on unit area and unit length of path and one based on the actual area and length of path for the particular transformer; but this still represents a point-by-point method, whereas the template method gives complete curves at one calculation.

Mr. Woods brings out the point that, in the majority of calculations involving current transformers for relaying service, there is very little need for phase-angle data. This point might be emphasized more strongly by an analysis of relay operation. Only those relays that involve the comparison of two currents, or a current and a voltage, are affected by both ratio- and phase-angle errors. Most relays that compare two currents have restraint coils that permit a discrepancy between the two currents. In the case of a differential relay having a 25 per cent slope characteristic, operation will be successful from two current transformers that have a maximum *RCF* difference of 0.25. These relays purposely are made insensitive to phase-angle differences between the two currents. True impedance relays are, of course, independent of phase angle. Reactance relays will develop an error in terms of the line phase angle, which for a fault is usually around 75 degrees. This results in an error, for example, of sin 75 compared with sin 69 (six degrees difference as Mr. Woods pointed out as maximum) or 3.2 per cent error under the worst condition. The ratio error for such a condition, as pointed out by Mr. Woods, would be in the neighborhood of ten per cent. This 3.2 per cent error, introduced by ignoring the phase angle, would be compensated for by the pessimistic results of inphase addition, which would be in the neighborhood of 3.5 to 4 per cent. The conclusion is reached that, of the two inherent errors, the ratio error is by far the determining factor in those relay applications that involve both. It seems possible that, as this becomes more widely known, there will be little or no need for phase-angle data for any relaying application.

Mr. Brownlee's excellent analysis of the ground-relaying problem leaves little more to be said on the subject with the exception that the method chosen depends secondarily upon the location of the fault with respect to the transformer, the source, and the grounding point, and primarily upon the magnitude of the ground burden with respect to the phase burden. If this ratio is large (10 or more), which is usually the case, it does not matter which method is used, since all three transformers are effectively in parallel across the ground burden, and the phase burden is then negligible compared with the ground burden.

REFERENCES

1. P. G. Agnew. Bureau of Standards *Bulletin*, volume 7, 1911, page 432.

2. A SIMPLE METHOD FOR DETERMINATION OF RATIO ERROR AND PHASE ANGLE IN CURRENT TRANSFORMERS, E. C. Wentz. AIEE TRANSACTIONS, volume 60, 1941, October section, pages 949–54.

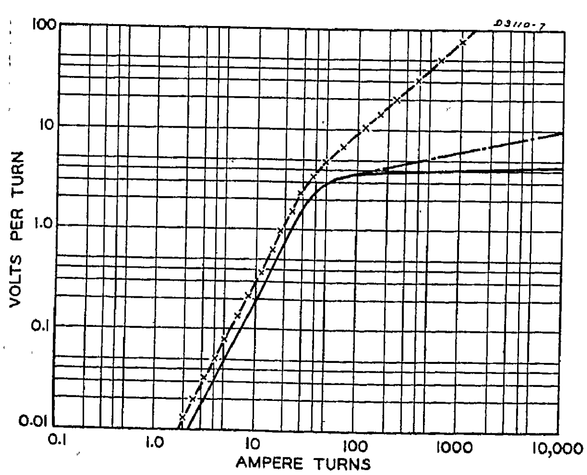


Figure 7. Saturation characteristic curves taken with sine-wave voltage and with sine-wave current

- - - - Sine-wave voltage and any voltmeter
——— Sine-wave current and rectifier-type voltmeter
—·— Sine-wave current and iron-vane-type voltmeter
—x— Sine-wave current and peak-type voltmeter

Report on Application of Lightning Protective Devices in Wartime

Discussion and closure of paper 43-111 by the lightning arrester subcommittee of th AIEE committee on protective devices, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, September section, pages 586–8.

Edward Beck (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The scope of the report, it seems to me, is broader than stated. It embraces more than the material involved in different types of arresters, or the maintenance of arresters, or lowering the rating of arresters already installed. The purpose of protection is to assure that there will be no interference by lightning with the delivery of power, and that this object be accomplished with the minimum expenditure of material and man-hours of maintenance or repair on the system. This is the scope of the report.

Under "Rebuilding and Revamping Old Arresters" it is apparently the purpose of the report as written to recommend merely the reducing of ratings of arresters by removing sections. Where this is safe, it is an improvement that involves no critical material. If, however, by revamping and rebuilding is meant the substitution of modern up-to-date elements in obsolete arresters, it will generally be found that less critical material will be involved in replacing such obsolete arresters by complete new ones.

In connection with mention of protective devices other than arresters, we believe it should be pointed out that devices that do not incorporate circuit-clearing features, such as rod gaps, are not, strictly speaking, lightning arresters because they will not prevent system outage unless they are assisted by other equipment such as fuses and circuit breakers.

There is a subject that has not been covered in the report which we believe should not be omitted, especially since the report puts particular stress on the conservation of material. It has to do with the lightningproofing of transmission lines. This is accomplished successfully by one of two means: overhead ground wires or protector tubes. In the past it has been logical to make the choice of one or the other on the basis of cost. Frequently, especially in new construction, the overhead ground wire has the advantage from this standpoint. However, it should be borne in mind that the amount of critical material involved in protector-tube installations will generally be less than the amount of critical material involved in an adequate overhead ground-wire installation. Consideration should be given to this in the design of new lines or in the rebuilding of existing ones, and I believe that a report on conservation of critical material in lightning-protective devices in wartime should call attention to this matter.

I. W. Gross (chairman, lightning arrester subcommittee 1942–43): Since the purpose of the committee report was to bring to the attention of the industry some features worth while in conserving materials and man-hours in the war effort, it is quite gratifying to find that Mr. Beck's discussion has amplified to some extent a few of the points mentioned in the paper, but not discussed fully therein.

The scope of the report necessarily had to be brief and concise, but as pointed out by Mr. Beck, application of one or more of the suggestions mentioned may have a much broader aspect than the mere saving of critical material; for example, maintenance of electrical service to a standard acceptable under war conditions.

On the subject of "Rebuilding and Revamping of Old Arresters," of the oxide-film type at least, there is involved a considerable increase in protection of equipment as well as a saving or salvaging of usable material, such as the oxide-film cell units. While it may not be possible in most cases to rebuild these salvaged units into new arresters, they are available for replacement when and if the cells in service become defective, as found on periodic inspection, thus not requiring new material in such servicing.

It is rather difficult to visualize how there is any saving in critical material by substituting new arresters for old ones, unless it is maintained that the salvage from the old arresters will more than compensate for the material used in the new ones. This, however, does not seem to be a very plausible basis for analysis.

The point made by Mr. Beck that lightning arresters, strictly speaking, are devices which on functioning will not cause interruption of power flow on the system, while other devices such as rod gaps may result in outages, is well taken. As mentioned in the report, however, the preference in the choice of an arrester as against a similar device which may result in a momentary power interruption is outside the scope of the report.

While the subject of protecting transmission lines from lightning by such means as shielding with ground wires or the use of protector tubes is of importance in any system layout, just how far to go in any particular case is one of individual judgment and local conditions.

This report limited its scope to protective devices as such and to their general application in the protection of various types of devices and equipment, and not to the protection of the electric system as a whole. There are many other ways of dealing with lightning on a transmission line; for example, by high speed reclosing breakers, use of ground wires, counterpoises, high insulation, and so forth. Just which of these devices or methods to use, if any, is again a matter involving a study of local conditions and individual judgment in each case.

Frequency Modulation for Power-Line Carrier Current

Discussion of paper 43-112 by E. W. Kenefake, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 616–20.

E. H. Bancker (General Electric Company, Philadelphia, Pa.): Although carrier-current equipment has become fairly well standardized, this does not mean that development has ceased. It does mean that the talent of the engineering force that previously was devoted to engineering each application has been released for investigation of ways to improve the standard product. Mr. Kenefake's paper gives some of the early results of such an investigation and points the way to what may be in store in the future. To those of us who have been impressed with the performance of frequency-modulated radiobroadcasts in comparison with amplitude modulation, Mr. Kenefake's paper answers our question as to whether, and how, the same good results could not be secured in the carrier field. You will undoubtedly agree that the results of the experiment indicate that reduction in power requirement or increase in range of successful operation are not very remote possibilities in the carrier field.

Aircraft Electric Power-Supply System

Discussion and author's closure of paper 43-123 by J. E. Yarmack, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 655–8.

E. C. Wentz (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): To those who have been associated closely with the aircraft industry, the accomplishments set forth by Mr. Yarmack may not seem remarkable, but a comparison of Mr. Yarmack's transformer with the ordinary dry-type transformer will emphasize the magnitude of his accomplishment. For instance, we recently redesigned our standard dry-type transformers and reduced the weight of active material in the 25-kva 60-cycle design to 218 pounds, which we still think is very good by ordinary standards, but Mr. Yarmack gets 33.7 kva in 30 pounds total weight.

The interesting conclusion is that the transformer engineer must forget nearly all he knows about transformer design before he begins to design an aircraft transformer. It is still one of the almost unexplored fields of engineering. Not nearly as much has been done as will be accomplished in the future.

As an illustration, a design for the same service which we made, after Mr. Yarmack had made his, put out 36 kva and weighed only 25 pounds. Its iron loss was 230 compared with Mr. Yarmack's 320, and our full-load copper loss was 1,420 (at 75 degrees centigrade) compared with 1,230. This is not by way of disparagement of Mr. Yarmack's accomplishment; it merely illustrates the rapid progress in the industry and the opportunities still open to the designer. Mr. Yarmack can undoubtedly produce figures on a newer design, even lighter and more efficient than ours.

It is interesting to note the relatively high efficiencies of these new transformers. The losses in a conventional 25-kva 60-cycle design are 140 iron and 410 copper compared with 230 and 1,420. The increase in loss does not go in inverse proportion to the decrease in weight.

The greatest reduction in size comes from the higher frequency of 400 cycles. Hipersil, in the thickness of 0.007 inch, can be worked at nearly saturation density at 400 cycles without excessive loss. As far as the transformer industry is concerned, the introduction of Hipersil means that 60 cycles is now a most uneconomical frequency. For aircraft transformers it now appears that 600 or 800 cycles would be more economical.

These transformers are air-blast cooled and operated at about 80 degrees centigrade temperature rise, which is convenient because the rectifier needs the air blast anyway. Nearly as good results can be attained in self-cooled designs if the newer insulating materials, allowing temperatures up to 250 degrees centigrade, are used. It is doubtful that air-blast transformers will be worked normally at 250 degrees centigrade because of even lower efficiencies and because of the hazards of working copper so hard that a few seconds of interruption of the air blast will melt the windings.

Mr. Yarmack has done a notable job in organizing and successfully completing a development which presented so many new problems.

H. J. Braun and C. G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Rectified alternating current, as a source of d-c power for aircraft use, has long been an intriguing possibility. Mr. Yarmack's paper summarizes some of the more recent attempts to use selenium rectifiers for this purpose and represents a worthwhile contribution to the art. It may be well to point out, however, that there are other types of rectifiers which also have possibilities for this application.

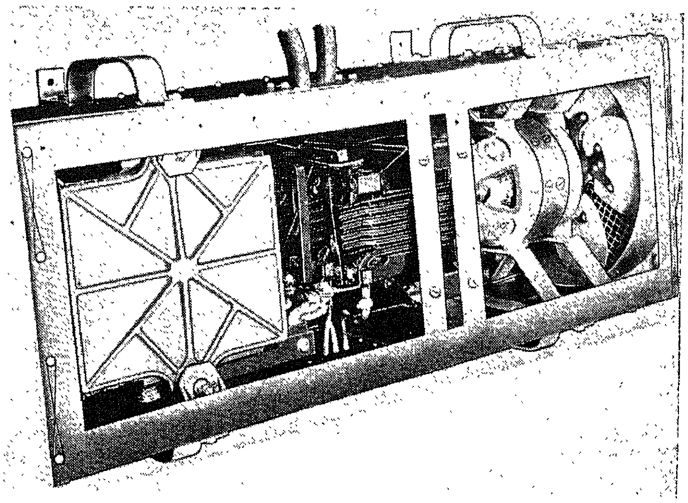


Figure 4. Fan-cooled transformer–rectifier unit for aircraft service

Rated 12 kw

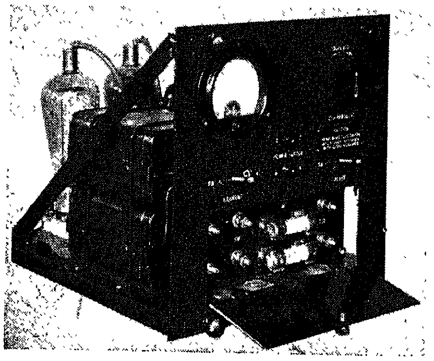


Figure 1. Aircraft transformer–rectifier unit

Built in 1930. Output 1,000 volts, 400 milliamperes

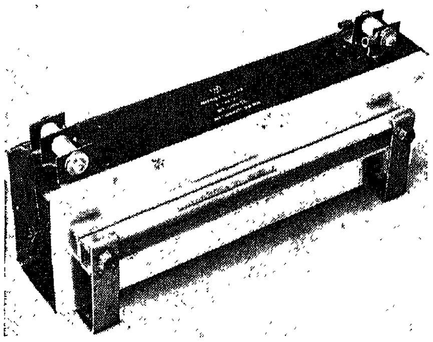


Figure 2. Aircraft rectifier

Rated at 200 amperes, 30 volts. Weight 13 pounds. Built in 1942

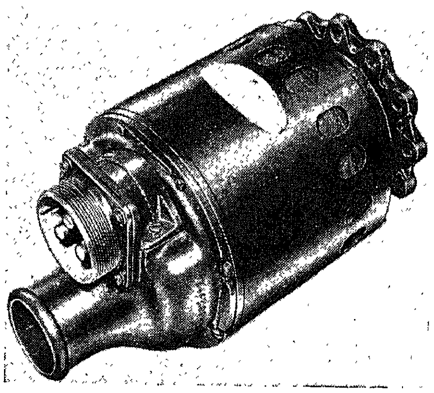


Figure 3. Engine-mounted aircraft alternator made to supply power for rectifier shown in Figure 2

In 1930 operating experience of the United States Navy demonstrated that a-c generators were far more reliable for aircraft use than the 1,000-volt d-c generators it was then using. At their request we developed a rectified a-c system delivering 1,000-volts direct current. For this system a mercury-vapor rectifier, shown in Figure 1 of this discussion, was used.

More recently, because of the low d-c voltages required, we have devoted our attention to the use of dry-plate rectifiers. While both selenium and copper-oxide rectifiers have been considered, most of our activity has been with the copper-oxide type.

During the winter of 1941–42, a 200-ampere 30-volt rectified a-c system was developed. The rectifier built for this system used copper-oxide plates and was cooled by a blast tube. It weighed only 13 pounds. Figure 2 of this discussion shows this rectifier. The a-c power for this system was furnished by an alternator which weighed 24.6 pounds and which operated at a speed of 5,000 to 10,000 rpm. Figure 3 of the discussion shows this alternator. The weight of the total system was 37.6 pounds which compares quite favorably with the 36-pound weight of the equivalent d-c machine.

Later in 1942 a 400-ampere rectified a-c system was developed. The rectifier in this system was part of a packaged unit containing a transformer and a motor-driven cooling fan. This unit was probably the first of its kind ever built for aircraft use. Figure 4 of this discussion shows this complete unit with the cover removed. The component parts are clearly visible It has been tested to a pressure altitude of 39,000 feet.

Since both copper-oxide and selenium rectifiers offer promising possibilities for use in aircraft, some comparisons may be in order. Recently we have built rectifying elements for a 200-ampere copper-oxide rectifier. A comparison with the data in Mr. Yarmack's Figures 3, 4, and 5 shows practically identical efficiency and weight, but the copper-oxide elements occupy a space of 0.14 cubic foot compared with Mr. Yarmack's "less than 0.25 cubic foot."

One of the original reasons for giving serious consideration to the use of rectified alternating current as a possible alternate to a d-c generator was the extreme difficulties then being encountered with excessive brush wear at high altitudes. So much progress has now been made in the solution of the high-altitude brush problem that rectified alternating current is not now so likely to replace d-c generators as a main source of electric power as it might have been had progress not been made in the brush problem.

C. C. Horstman (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): Mr. Yarmack has presented a very interesting solution to the problem of obtaining large power-plant capacity in aircraft. His solution for obtaining a very light transformer has been especially interesting to us.

As stated by Mr. Yarmack, he achieved this result by using a very unique design with the latest kind of material. The advent of the new low-loss high-permeability oriented type of electrical steels has been timed most fortunately to coincide with the demands for lighter transformers. Mr. Yarmack has pointed out that in a transformer for aircraft it is most important to have minimum weight, but it is also important to have high efficiency and satisfactory service.

For some years we have had Hipersil core material in standard 29-gauge thickness for use in power transformers. This material is characterized by very low losses and high permeability in the rolling or grain direction. These results are obtained by a carefully controlled rolling and heat-treating cycle, such that the crystals of silicon steel are oriented preferably with an easy direction of magnetization in the rolling direction.

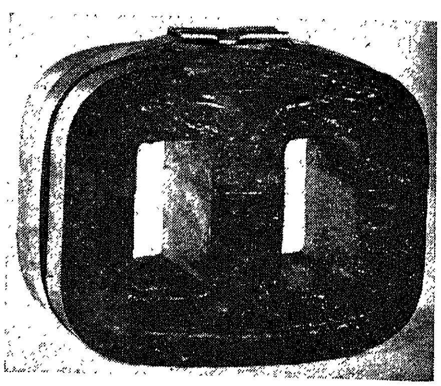


Figure 5. Hipersil type-C three-phase core

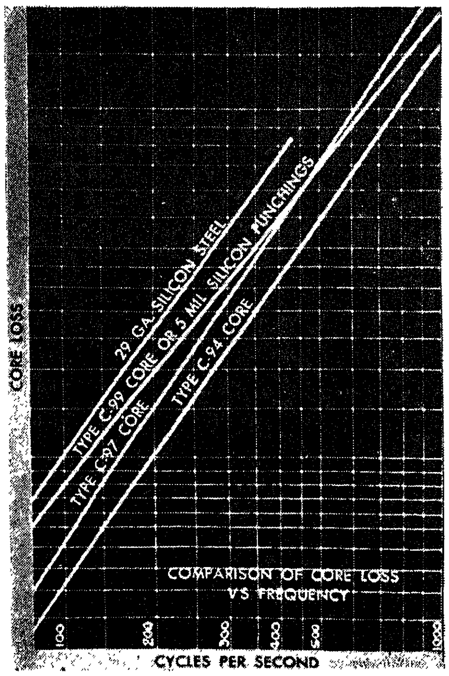
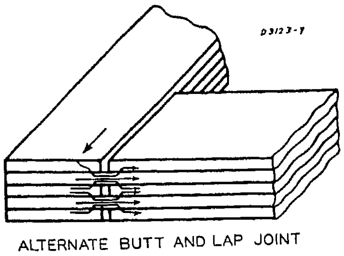
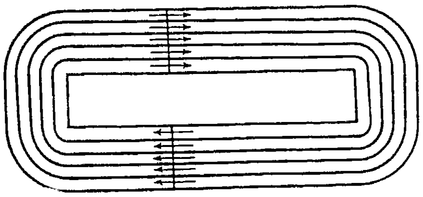


Figure 6. Comparative core loss of Hipersil conventional cores



ALTERNATE BUTT AND LAP JOINT



HIPERSIL CORE BUTT JOINT

Figure 7. Comparison of Hipersil core butt joint with the conventional alternate butt and lap joint

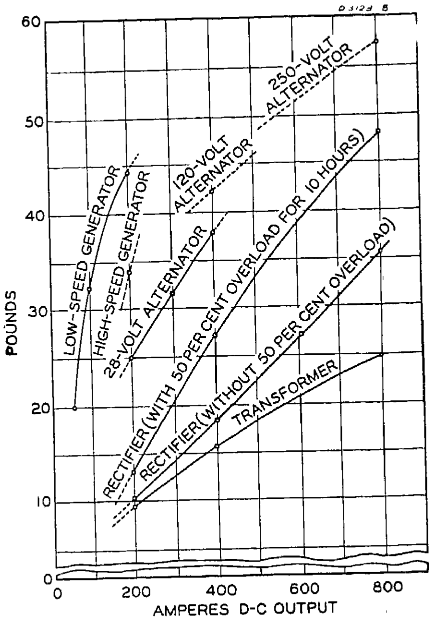


Figure 8. Relation of weights of aircraft electric-power generating equipment to the d-c output current

In order to make efficient use of these good magnetic properties in the grain direction, it is necessary that the material be made into a core so that all the flux coincides with the grain direction.

This construction is illustrated in Figure 5 of this discussion. The core is made by winding two loop sections with a third section wound on the outside of the two, the whole core annealed at high temperature, impregnated, bonded solidly together, and then cut in two to form two pieces for ease of assembly. In this type of construction all the core material is active, resulting in a minimum weight for the core. Mr. Yarmack has shown the complete simple framing construction possible in his paper.

For use in this type of transformer operating at 350 cycles and upwards, Hipersil steel has been developed to have preferred orientation down to a thickness of seven mils. It is only by using a core material of this thickness with the very low hysteresis losses and high permeability of Hipersil that the unique features of the design can be achieved. Figure 6 of this discussion is a comparison of the core losses of this type of core compared with standard cores; it illustrates the large increases in working inductions that are possible with this core material.

It might be well to mention that the butt joint used in this type core is very beneficial at the high frequencies and high flux densities involved. Figure 7 of this discussion will illustrate this point showing how the butt joint of the Hipersil core eliminates crowding of flux in every other lamination of a conventional alternate butt and lap joint. At high frequencies or high densities this flux crowding will cause early saturation, high losses, and high magnetizing current.

Mr. Yarmack has very ingeniously incorporated this new thin-oriented-Hipersil three-phase core into a transformer having high efficiency and minimum weight. The reduced section of iron has enabled him to develop a very clever design for the copper coil and by careful design of all features has produced a transformer that will fit very well with the lightweight rectifier into an a-c–d-c aircraft system.

J. E. Yarmack: The comments expressed by Mr. Horstman and Mr. Wentz are gratifying, since they amplify the transformer-core data. They are also welcome because they represent additional valuable remarks on the properties of Hipersil. This type of transformer core will play an important role in the electrical industry and, in even a greater degree, among the electrical engineers participating in the aviation industry.

Table I. Approximate Weight Comparison of Component Parts in Three Aircraft Power Schemes

3-Kw Direct Current and 1.5-Kva Alternating Current—D-C System (P-2)		6-Kw Direct Current and 1.5-Kva Alternating Current—A-C–D-C System (400 Cycles)		6-Kw Direct Current and 1.2-Kw Alternating Current —NEA-5, U. S. Navy (800 Cycles)	
Equipment	Pounds	Equipment	Pounds	Equipment	Pounds
D-c generator	35	Alternator	30	A-c–d-c generator	45
Regulator	3	Rectifier	10	A-c regulator	3
Current relay	2	A-c regulator and transformer	10	D-c regulator	3
1.5-kva inverter	35	D-c regulator	3	Current relay	2
Total	75	Total	53	Total	53

The discussion by Braun and Veinott should be taken only with full cognizance of the fact that the author's paper represents a report as of the end of 1942. A selenium rectifier of much smaller size and weight than the one illustrated in the paper was already available during the first half of this year. It did not seem desirable, however, to make emphatic engineering statements until after the completion of extensive tests and experiments.

Advocation of the a-c–d-c system as a substitute for the straight *P*-2 system may be viewed in the light of characteristics shown in Figure 8 of this discussion. The weights shown on this plot may deviate somewhat and are being cited with due respect for the skill of individual designers and manufacturers. The steepness of the curve illustrating the weights of d-c generators indicates that the weights of d-c machines of higher than 200-ampere rating are prohibitingly greater if they are flange-mounted, that is, according to present practice. One of the possible and very feasible

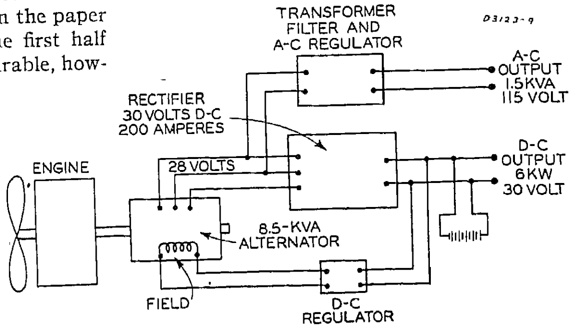

TRANSFORMER FILTER AND A-C REGULATOR
D3123-9
A-C OUTPUT 1.5KVA 115 VOLT
RECTIFIER 30 VOLTS D-C 200 AMPERES
ENGINE
28 VOLTS
D-C OUTPUT 6KW 30 VOLT
8.5-KVA ALTERNATOR
FIELD
D-C REGULATOR

Figure 9. Schematic diagram of aircraft electric-power plant utilizing alternator, rectifier, and also a-c and d-c regulators. The system gives dual output

solutions could be an a-c–d-c system utilizing alternators, rectifiers, transformers, and both a-c and d-c regulators (Figure 9 of this discussion). Such a system could be useful in an aircraft employing a-c-operated apparatus. As far as weights are concerned, the system offers considerable advantages (Table I of this discussion) and, at the same time, gives a marked increase in the total amount of electric power. It is of interest, however, to point out the similar weight analysis of the 800 cycles per second system employing an a-c–d-c generator that weighs 45 pounds. The latter plant, on the other hand, being of 800 cycles, adapts itself more readily to a smaller-size aircraft where radio equipment, rather than power equipment such as electric motors, is prevailing.

Characteristics and Applications of Selenium-Rectifier Cells

Discussion and author's closure of paper 43-124 by Edgar A. Harty, presented at the AIEE national technical meeting, Cleveland Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 624–9.

C. A. Kotterman (consulting engineer, New York, N. Y.): The author of this interesting paper states in his introduction that a manufacturing plant has been set up which permits producing cells in large quantities within close electrical tolerances. Yet, in his introduction to "Electrical Characteristics of the Elements," he states that "it is impossible to make all cells exactly alike as to characteristics. . . ." "A slight deviation should be expected between individual cells." It would be very helpful to users of rectifiers if they could be given something more concrete about this deviation instead of the rather indefinite statements in the paper.

Aging

The increase in forward resistance with time of operation of metal-plate rectifiers is known as aging and is common to all such types of rectifiers, although different types age at different rates.

Mr. Harty refers to this phenomenon of aging at least five times in his paper, but, unfortunately, he does not give us any quantitative information on this point. Another manufacturer of selenium rectifiers in the United States asserts that the forward resistance of his elements increases approximately 50 per cent during the first 10,000 hours of operation. I am sure some further statement from the speaker regarding this important matter of aging would be of general interest and very helpful to those of us who are daily designing equipment using the selenium rectifier.

Relation of Rectifier Resistance to Total Resistance of the Associated Circuit

It has been stated that, to minimize the effect of rectifier resistance on the rectifier circuit, the recommended ratings of the General Electric selenium rectifier have been chosen so as to make the rectifier resistance about 10 per cent to 15 per cent of the circuit resistance. How does this hold for battery charging?

Rectifier Design

In the example cited by the author on page 629, he states that, for a 30-volt one-ampere rectifier, three is the minimum number of bridges to be selected. Should that not be the minimum number of cells in series? If "bridge" is really meant, in light of the author's definition of unit rectifier, then I believe that the rectifier nomenclature will be confused rather than clarified by the use of "bridge." In my experience bridge has always referred to a single-phase full-wave rectifier consisting of one or more rectifier elements in series per arm.

Life Tests

The published life-test data look very good. However, the data are not of very much practical value to the users of rectifiers, because they do not give the ambient temperature in which the tests were run, nor the current density. Were the test stacks derated in any way? As the data represent constant load, what sort of curves would be expected for constant current? What percentage of increase in reverse resistance would be expected along with the increase in forward resistance? What sort of curves could be anticipated where the current density per unit area is above normal, say up to 2.5 times the normal current rating?

In view of the interest shown in the heavy-current metal-plate rectifier application to d-c power supplies for aircraft, would Mr. Harty care to discuss the probable trend of these life-test curves where the current density is between two and three times normal and the frequency varying between 200 and 800 cycles?

H. J. Braun and **C. G. Veinott** (Westinghouse Electric and Manufacturing Company, Lima, Ohio): E. A. Harty has presented many useful technical data on the characteristics of selenium cells. These data should prove of value to engineers in applying these cells.

Mr. Harty has used a symbolic notation for the various rectifier circuits different from the one with which we are familiar. In the interests of standardization, a common language in speaking of the various metallic-rectifier circuits would be highly desirable. We have been using a notation based upon the mnemonic word *ASP*, where *A* stands for the number of arms in the rectifier, *S* stands for the number of cells in series in each arm, and *P* stands for the number of cells in parallel in each arm. Thus a three-phase bridge circuit would be represented by the notation 6–1–1, or 6–3–5, or 6–*S*–*P*, depending, of course, upon the necessary combination of plates to handle the voltage and current requirements of the completed rectifier.

For further use of this system by other people see reference 1.

Reference

1. Selenium Rectifiers and Their Design, John E. Yarmack. AIEE Transactions, volume 61, 1942, July section, pages 488–95.

Edgar A. Harty: The author expresses thanks to H. J. Braun, C. G. Veinott, and C. A. Kotterman for their comments.

The symbolic notation used by the manufacturers of copper-oxide rectifier cells was based on the formula "arms-series-parallel" where a figure showed the number of arms in the circuit, another one the series cells, and finally a third one the cells in parallel. For example, 4–1–1 represented a single-phase full-wave bridge circuit using one cell per arm. A three-phase full-wave bridge circuit was indicated by 6–1–1. However, a two-phase center-tap circuit also used 4–1–1, and all three-phase circuits shown in Figure 19, except the half-wave one, used 6–1–1.

The symbolic notation used in this paper avoids this duplication and also attempts to offer a notation which not only describes the circuit but also shows how the cells are functioning.

In connection with the variation between cells, our one-square-inch cells are held between plus or minus one milliampere at 15 volts for leakage values, as shown in Figure 2, and plus or minus 0.05 volt at 0.5 ampere in the forward direction.

All aging curves are based on tests running at full-rated load and at ambients around 27 degrees centigrade. For the duration of these tests the forward resistance of each cell measured at 0.5 ampere changed only 18 per cent.

In applying cells to battery-charging circuits, a certain amount of ballast must be used in the form of reactance to minimize aging.

When overloading cells, for the same change in resistance, aging will be greater at the overload point. It is, therefore, very important to rerate the cells to a lower voltage to obtain a reasonable life.

Finally, an article by A. E. Richards[1] should have been included in the list of references. This article is highly recommended, although describing a different make of selenium rectifier.

Reference

1. The Characteristics and Applications of the Selenium Rectifier, A. E. Richards, *Journal* of the Institution of Electrical Engineers (London, England), volume 88, October 1941.

Fundamental Principles of Amplidyne Applications

Discussion and author's closure of paper 43-125 by F. E. Crever, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, September section, pages 603–06.

W. H. Formhals (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The word "Amplidyne" is the trade name given a form of the Rosenberg generator or Pestarini metadyne when operated as a regulator or control device and is so used in this paper. In view of this, it is believed that the subjects of terminal markings, polarity, and so forth, must be considered primarily from the aspect of its use as a rotating regulator.

On this basis it would appear that the tabulation of Figure 5 would be of value to a design engineer as a standard sheet for use in his own organization, but it has no place in the technical literature where it might be misunderstood as setting up standards for terminal markings for all rotating machines used as regulators and control devices.

The Rototrol which was the pioneer among rotating regulators has always followed the National Electrical Manufacturers Association and AIEE Standards for terminal markings for both clockwise and counterclockwise rotation. In contrast to these standards the armature polarity of the Amplidyne does not reverse with change of rotation as in d-c machines, and there are numerous rules to observe concerning the necessity for reversal of field leads.

Since the rotating regulator is a control element and since each is designed for an individual job, it is believed that it would be better for the users to rely entirely upon the control diagram for the connections and use of each of the many fields rather than to feel that fields with a given marking have a specific function in all cases.

We therefore believe that this paper should not be construed as setting up a set of standard markings for rotating regulators.

F. E. Crever: A chief purpose of my paper was to point out how a rotating machine is used as an amplifier in regulating systems. The paper clearly shows that the machine itself is not a complete regulator, but rather a new form of power amplifier. Hence, the name "rotating regulator" applied to the Amplidyne by Mr. Formhals seems inappropriate.

The terminal markings shown in Figure 5 of the paper apply to the use of the Amplidyne generator as it has developed up to the present time. They were included in the paper so as to give as clear as possible an understanding of the Amplidyne circuits and avoid confusion in describing their applications to a great variety of industrial and other uses.

Design Relationships for D-C Generators for Use in Aircraft

Discussion and author's closure of paper 43-126 by S. R. Bergman, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 613–16.

A. Fisher and F. M. Potter (General Electric Company, Lynn, Mass.): The design relationships for aircraft-type generators follow the fundamental principles of d-c machine design, in that the performance improves in the following order:

1. Noncommutating-pole generator.
2. Commutating-pole generator.
3. Compensating (and commutating-pole) generator.

Normally, for small conventional constant-speed generators, the costs increase in the same order with the commutating-pole type, the most economical from the standpoint of material. However, for the same performance, the compensated machine proves to be the most economical from both the weight and cost standpoint. For equal performance, it is our experience that the other types must be larger and never, within practical limitations, quite approaching the same performance.

The design described in this paper lends itself to quantity production so that the type of winding affects the over-all costs to a relatively small degree, the time, labor, and materials required for manufacture being determined mostly by necessary mechanical considerations. Although the compensating winding would seemingly increase the copper weight somewhat, it does perform a double function in that it surrounds the commutating poles, the net effect being a somewhat greater mean length of turn for the commutating-pole winding. Inasmuch as the overcompensation required by a compensated machine is considerably reduced, and also since it is possible to use small air gaps and reduced weight of shunt-field copper, the total net weight of copper and watts copper loss are actually reduced.

The many problems connected with aircraft-generator design are simplified greatly by the following machine advantages:

1. Ability to carry heavy overloads with good commutation over the entire speed range.
2. Good stability when operated with a voltage regulator with wide and sudden changes in load and speed.
3. Lower temperature rises resulting from
   (*a*). Good distribution of windings.
   (*b*). Lower load losses.
   (*c*). Lower commutator and brush losses.
   (*d*). Improved methods of ventilation.

The progress made in aircraft-generator design will be of considerable use in future industrial-machine design. We have built a number of compensated-type machines which have shown up to advantage, particularly in regard to low time constants and linear relationship of torque and current. The greatest contribution will be from the standpoint of improved insulations and lightweight design.

N. R. Schultz (General Electric Company, Schenectady, N. Y.): In general, the load-voltage drop of a self-excited shunt generator is caused by

(*a*). Armature resistance drop.

(*b*). Demagnetizing effect of armature reaction.

(*c*). Decrease in excitation because of *a* and *b*; that is, with a given value of shunt-field circuit resistance.

In the case of a compensated generator, the function of the compensating windings is to neutralize wholly or partially the effects of armature reaction. Since the demagnetizing effect of armature reaction increases directly (neglecting saturation) with armature current, better overload characteristics are obtained with the compensated generator than with the uncompensated generator.

Laboratory tests were made on compensated and uncompensated air-corps-type *P*-1 generators rated 200 amperes, 2,500/4,500 rpm to determine the relative characteristics of compensated and uncompensated machines. The following summarizes the general results obtained:

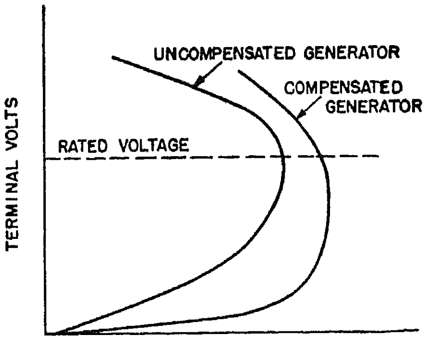


Figure 1

1. A comparison of the volt-ampere characteristics of the compensated and uncompensated generators, as obtained by test, indicates that at a given speed the compensated generator is able to produce more overload power than the uncompensated generator. This may be important during emergency conditions when one or more generators of a multiengined aircraft are out of service. Typical volt-ampere characteristics of the compensated and uncompensated generators are shown in Figure 1 of this discussion. These curves are representative of generator operation at a speed approximately midway in the rated speed range.

2. Oscillographic records of laboratory tests show that the compensated generator is capable of supplying more current at higher voltages to heavy shock loads than is the uncompensated generator under the same conditions. On actual aircraft electric systems, this characteristic of compensated generators has the following advantages:
   (*a*). Smaller system voltage dips due to sudden applications of load.
   (*b*). More rapid acceleration of motor loads.
   (*c*). Lesser dependence upon batteries for assistance in supplying heavy shock loads.

3. Commutation characteristics of the compensated and uncompensated generators were observed while the machines were operated in an altitude chamber which simulated atmospheric pressure and temperature conditions found at 35,000 feet. It was observed that sudden application of approximately full load upon the uncompensated generator resulted in severe sparking at the brushes. Moderate sparking persisted as long as full load was held on the generator. When this test was repeated at sea-level conditions, the results obtained were quite similar except that the intensity of the sparking was somewhat reduced.

The high-altitude and sea-level tests were also made upon the compensated generator. Perfect "black" commutation was observed under all conditions, no sparking being perceptible upon sudden application or removal of full load.

It may be of interest to point out that in the course of our laboratory studies of the interrupting ability of reverse current contactors and small circuit breakers, we have applied several hundred severe short circuits to several compensated-type *P*-1 generators. These short circuits have been varied over the whole range of current and voltage conditions by varying the fault resistance. The generators have withstood these faults without any perceptible difficulty or resultant damage, and we have yet failed to flash over a compensated machine.

C. B. Mirick (Naval Research Laboratory, Washington, D. C.): The writer of this discussion has had opportunity to examine and test most of the machines described in this paper and is in general accord with the views expressed therein. The paragraph on weights seems particularly significant. A sudden weight reduction of approximately 90 per cent for a type of mechanism which has been in general use for many years is a startling achievement. One might expect that it would have a profound effect on future design of rotating electric machinery.

C. J. Fechheimer (Louis Allis Company, Milwaukee, Wis.): There is a danger of operating machines at very high temperatures, entirely aside from the possibility of insulation failure. The reader is referred to my discussion of Henry M. Hobart's paper.[1]

REFERENCE

1. CRITICAL-MATERIAL CONSERVATION IN INDUCTION-MOTOR MANUFACTURE, Henry M. Hobart. AIEE TRANSACTIONS, volume 62, 1943, August section, pages 549–52.

J. D. Miner (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Doctor Bergman is to be commended for the excellent job he has done in demonstrating that armature compensation can be provided in compact high-capacity machines. It is obvious that the variable-speed weak-field requirements for aircraft generators indicate the use of armature compensation if it can possibly be provided. The writer designed compensated high-capacity generators when the first tentative specifications for 100-ampere and 200-ampere 30-volt generators were issued in 1940. It was considerably later that we built any compensated generators, because the calculations were unfavorable from weight considerations, since two factors in these early designs were insufficiently appreciated:

1. The amount of benefit which could be obtained from pole-face windings concentrated in a single coil per pole.

2. The amount of compounding effect which could be obtained by overcompensation.

Generators without commutating windings usually require advancing of the brushes so that commutation takes place under the main field. Advancement of the brushes results in a demagnetizing component of the armature magnetomotive force, in other words, a differential compounding effect. In addition, the resultant field in which the coils are commutated is usually insufficient to prevent circulating currents which also cause a differential compounding effect. It is common practice to attempt to balance demagnetizing effects by a weak cumulative series field, but the amount of series field that can be used is limited by the requirements for stability at high speeds. Moreover, in case of accidental closure of the reverse-current relay with the generator at standstill, the reverse current through a strong series field might well reverse the polarization of the machine.

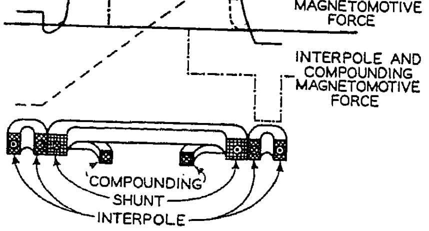

RESULTANT FLUX
D3126-2
ARMATURE MAGNETOMOTIVE FORCE
EXCITATION MAGNETOMOTIVE FORCE
INTERPOLE AND COMPOUNDING MAGNETOMOTIVE FORCE
COMPOUNDING
SHUNT
INTERPOLE

Figure 2. Effect of compensation with one coil (two coil sides) per pole

Generators with commutating poles may be designed so that an excess of commutating flux is obtained at normal load, and it will then be found that circulating currents will be produced which have the effect of a cumulative series field. Usually a satisfactory amount of compounding can be obtained without much difficulty with commutation, and it is well recognized that overcompensation gives good commutation at excessive loads by reducing the effect of saturation of the commutating poles. The compounding effect must be limited to a small amount, or instability will result at high speeds.

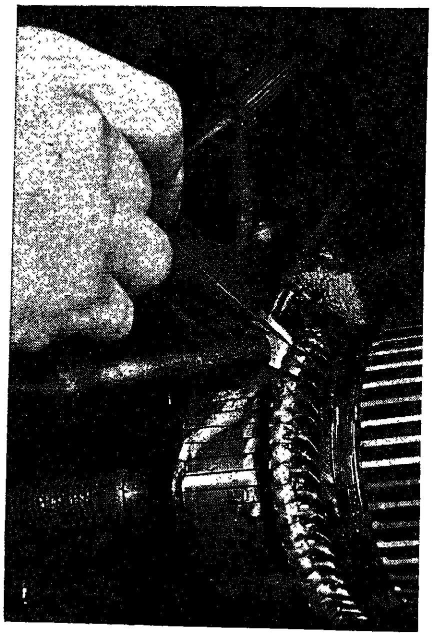


Figure 3. Brazing commutators

It has been found that an uncompensated machine requiring about six pounds of copper for excitation would require only $2^1/_2$ pounds of field copper if compensated, because of the saving in ampere turns required to overcome distortion and demagnetizing effects. However, such a machine will require poles which are about one pound heavier, interpoles and interpole windings weighing a total of about two pounds, pole-face windings weighing about one pound, and additional connections probably weighing one-fourth pound so that the net result is an increase in weight of nearly one pound. This additional pound must be balanced against the improvement in performance and overload capacity. If no improvement in output is desired, the armature can be reduced in size because of the lower losses, and a saving in weight can be realized in machines of high rating.

It must not be assumed that all aircraft generators should be compensated because, in the smaller sizes, the weight of a compensated machine will be greater than that of a satisfactory uncompensated machine. Military loads are of brief duration, and the average load with the turrets and similar equipment out of action may be but 15 or 20 per cent of the load when the aircraft is in battle. Very satisfactory performance has been obtained without compensating windings in generators as large as 200 amperes at 28.5 volts. Service records are available showing less than $^1/_{16}$-inch brush wear in well over 1,000 hours of flying with loads encountered under training conditions.

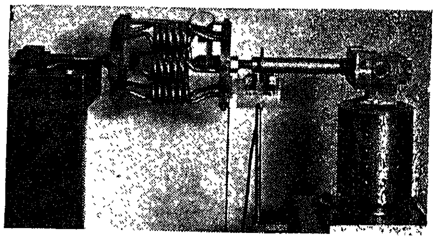


Figure 4. Fatigue-test setup

Mechanical considerations are important as well as is electrical design. Much of the success of any generator lies in the construction and design of the brush rigging and commutator. This is particularly true at the higher speeds, and the so-called high-speed (4,000/10,000-rpm) generators cannot possibly give satisfactory commutation and brush life unless the commutator remains mechanically stable at all operating speeds and temperatures. Rigid control of dimensions, materials, pressures, and processing are essential, as is, of course, an adequate mechanical design with which to start.

Doctor Bergman recommends brush temperatures below those corresponding to a 100-degree centigrade rise, and brush densities below 120 amperes per square inch. We believe that these values must be exceeded to obtain acceptable weight. Ventilation conditions are rarely as good as indicated by the specifications, and conditions have been encountered where the total head from the blast-tube cooling system was only one fifth the specification figure. Under these conditions we have obtained reasonably good brush life at 33,000 feet with brush temperatures in excess of 200 degrees centigrade. At these temperatures it is necessary to braze the commutator leads to the bars. A process was developed for doing this without damage to the hard copper bars when we started quantity production of high-capacity generators.

It is also believed that the quill-shaft and resilient-spindle construction is unnecessarily complicated. Two simpler types of resilient couplings have been developed. One of these uses bonded synthetic rubber. Although it will withstand operation for 30 minutes under this extreme condition, this coupling is not employed when operation at its critical speed is essential. In all practical cases it has been possible to cover the operation range without encountering critical speeds. A nonlinear spring coupling is also available; this does not have any critical speed but is somewhat heavier than the bonded synthetic coupling.

We also have developed apparatus for

fatigue testing of generator flange and flange screws. The development of a failureproof flange was relatively simple, but the elimination of flange screw failures was more difficult. The solution was found by means of the laboratory fatigue tests to lie in control of the yielding of the flange and not in the screws themselves. An endurance limit of not less than 10,000,000 cycles at 60g (where g is the acceleration due to gravity) has been found sufficient for mounting on any engine yet encountered.

Doctor Bergman failed to mention one feature of the compensated generator which is of considerable importance. This is that a generator of the compensated type is a very satisfactory motor and may be used for starting purposes when the generator is mounted on an auxiliary engine for ground power. A generator with brush shift or with a series field is not nearly so satisfactory when operated as a motor, since the brush shift is in the wrong direction, and the series field is differential.

S. R. Bergman: J. D. Miner points out that the compensated generator also may serve as a very good motor. That seemed so self-evident that it did not occur to me to discuss it. We are building compensated motors quite successfully for aircraft in sizes from one horsepower up to and including 15 horsepower.

One very important consideration lies in the fact that in compensated motors the starting current may be reduced considerably. The explanation is as follows:

Because of the neutralization of the armature reaction, we are able to design compensated motors with less number of poles than in the uncompensated type. The frequency and, therefore, the core loss are reduced. In addition, since there is no field distortion, the core loss is further reduced and runs usually less than one half of the core loss in uncompensated machines. Furthermore, because of the smaller number of poles, the excitation losses are reduced. Finally, the extra commutation losses are eliminated. Because of these reduced losses we can allow a much higher resistance in the armature circuit (armature in series with the compensated winding and the commutating poles) with a corresponding reduction in the starting current. In the smaller sizes we have actually cut the starting current in two, and in the larger sizes the starting current has been reduced from 30 to 40 per cent. This reduction in the starting current enables us in a great many cases to eliminate automatic starting devices, and we can start "directly on the line" without detrimental effect to the stability of the system.

Mr. Miner also brings up the question of brazing the armature leads to the commutator. This process is very attractive and has been used by us only in sizes over 300 amperes. We are, however, now tooling up to include both the 300- and 200-ampere generators.

Mr. Miner expresses the opinion that in smaller generators (200 amperes and below) compensation is unnecessary. We are building machines with compensation down to 50 amperes for generators and one horsepower for motors in quite acceptable weights.

Operation of Nonsalient-Pole-Type Generators Supplying a Rectifier Load

Discussion and author's closure of paper 43-114 by M. D. Ross and J. W. Batchelor, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 667–70.

R. D. Evans (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The heating effects in a generator carrying rectifier load have for some time been the subject of considerable speculation. Progress in solving this problem has come in two steps involving:

1. Determination of harmonics in the a-c system of rectifiers including generators.
2. Determination of machine heating that results from specific harmonic loading.

Harmonics in the a-c circuits of plain rectifiers were analyzed by Brown and Smith (reference 5 of the paper). The corresponding problems for rectifiers with phase control and for inverters were analyzed by Evans and Muller (reference 7 of the paper).

The increased use of rectifiers and the pessimistic report in European technical papers by Robert Pohl prompted the investigation reported in the paper by Ross and Batchelor. They have presented the results of harmonic tests on a typical generator supplying a rectifier under various conditions of phase control varying from zero degrees to 30 degrees delay. They have shown that the principal effect of the harmonics is an increase of the rotor temperature. They have described a method for calculating temperature rise, which is based on specific harmonic currents, the negative-sequence resistance of the machine, and the machine constants usually available for temperature-rise calculations.

The conclusions of this investigation are reassuring from the standpoint of heating in generators for the usual applications. Generators supplying rectifier load will have negligible additional temperature rise from harmonics if the rectifier load is only a small part of the total or if the rectifier is of 12 or more phases. The particular cases that deserve special study are those in which the generators are fully loaded with six-phase rectifiers or where the rectifiers are operated with phase control.

It may be of interest to describe an alternative method for determining harmonic loss in the rotor and estimating the additional temperature rise in the rotor that results from harmonic currents produced in an external source, such as a rectifier. In this method the harmonic-power inputs to the machine are measured and then reduced by the estimated harmonic losses in the stator. The resulting harmonic-power input to the rotor is then divided into components that produce rotor heating and shaft torque. This division is readily made by considering the machine operating as an induction motor at appropriate slips for the various harmonic frequencies. In this calculation the harmonics should be considered of different phase sequence, as mentioned by the authors, and should make use of equivalent circuits. The harmonic losses in the rotor are then used to calculate additional temperature rise in the usual manner, as illustrated by the authors. For the case of the generator supplying the six-phase rectifier, which was tested by Ross and Batchelor, harmonic-power-input measurements were made for the various amounts of phase control. The corresponding additional rotor temperature rises resulting from rectifier harmonics were estimated by the alternative method to be 17.2 degrees, 18.4 degrees, 20.9 degrees, and 27.2 degrees which may be compared with the test values of 19.1 degrees, 22.0 degrees, 22.1 degrees, and 25.5 degrees, listed in Table III of the paper, for the angles of phase delay from zero degrees to 30.4 degrees. It may be observed that this alternative method requires measurements of the harmonic-power input to the machine under load conditions or a corresponding estimate, whereas the method described by the author uses negative-sequence resistance which is generally available for the particular machine or from tests on similar units.

C. C. Herskind (General Electric Company, Schenectady, N. Y.): This paper is a valuable contribution in that it presents some data on generator heating caused by rectifiers. Past work and discussion on this problem have been handicapped by the lack of such data.

Since the penalty of generator overheating from this source is severe, engineers have been conservative. The authors' conclusions, that with a 12-phase rectifier additional heating will be slight and with more than 12 phases additional heating will be negligible, indicate that previous estimates of this effect may have been pessimistic. However, it should be recognized that each machine must be considered as a case by itself in determining the probable heating.

One installation has been made where a 200-kw rectifier is fed from a 350-kva generator. This rectifier transformer has a Scott-T primary and quarter-phase secondary connection. A-c filters were provided for harmonics 3, 5, and 7, and no trouble has been experienced caused by generator overheating.

When a 6- or 12-phase rectifier is supplied from a generator of approximately equal rating, the magnitude of the harmonics will generally be lower than in the case where the rectifier is supplied from a power system. This is due to the fact that the generator reactance is usually of the order of 10 per cent (on generator rating) while the power-system reactance may be one half or one quarter of this value. Since the magnitude of the harmonics is reduced as the total reactance of rectifier and a-c system is increased, the a-c harmonics will be lower when the rectifier is supplied from a generator.

The increase in total reactance, incurred when the rectifier is supplied from a generator of approximately equal rating, affects the rectifier characteristics. The high regulation drop and low power factor obtained on a six-phase rectifier under these conditions may mitigate against such applications and lead to a choice of a rectifier having 12 or more phases, thereby greatly reducing the probability of generator overheating.

M. D. Ross: Mr. Herskind points out that the harmonics are greater in magnitude when the rectifier is connected to a large system than when they operate on a single generator. The tests covered by this paper are, therefore, not representative as to the magnitude of the harmonics, but the increased harmonic values on a large system usually cause no concern, as they are spread over a number of generators and the increased heating of any one generator is small.

Mr. Evans' approach to this problem is very interesting and shows what can be done by careful measurement of the harmonics and their respective power factors. However, as he has pointed out, we have considered the problem from the standpoint of the machine designer who must predetermine the performance of a wide range of machines without benefit of test data.

After the paper was written some further study was made to determine the point of maximum loss on the rotor surface for each harmonic. The points of maximum loss were found to lie in a band covering 65 degrees to 75 degrees ahead of the pole center. There is no obvious explanation as to why these maximum points should lie so close together.

The Testing of Mercury-Arc Rectifiers

Discussion of paper 43-113 by H. L. Kellogg and C. C. Herskind, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, December section, pages 765–73.

J. H. Cox (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): I am going to point out a few differences between the practices of my associates and myself, and those described by Kellogg and Herskind. In so doing, I believe that I shall emphasize the value of the paper. Because of the number of still unknowns in rectifier design, it is highly desirable to publish testing procedures so that they may be compared, and through that process ultimately to establish correct methods and to enable the writing of Standards.

In the determination of the quality of a rectifier, the usual limitation is arc-back, and arc-backs are random phenomena. This means that the determination becomes the collection of statistics. The collection of statistics is usually a long and tedious process, and some from of accelerated test is attractive. We are experimenting with forms of accelerated tests, and I am most happy to know that others are doing likewise. However, I question whether the factors are well enough known at present so that any interpretation of the result of the accelerated test described by the authors could be considered very reliable. It may be noted that the rating applied to a particular design by Kellogg and Herskind is roughly one half of the continuous load limit termined by the ed accelerated test, and that such a test required two or three days. It has been my experience that a test that uses the normal duty cycle, includes the overloads specified for heavy-duty railway service, and extends for the order of three days, will provide a quite reliable basis for rating determination, and a basis which I think is as reliable as any accelerated test yet established. This would correspond most closely with the duty-cycle test described in the paper. The large amount of experience gained during the past three years indicates that the base load of the railway duty cycle is a satisfactory value for the continuous rating for electrochemical service.

Referring to the specific test described, I believe that a percentage of expected rating would be preferable to 100 or 200 amperes per step in order to make the test more consistent for various sizes of rectifiers.

I question the value of the current-limit test. It is somewhat complicated, and I do not see that it indicates anything not shown by the simpler short-circuit and simulated arc-back tests which are closer to service conditions. Simulated arc-back tests can be made to duplicate actual arc-backs more closely by the use of an ignitron tube connected backwards in place of the short-circuiting switch and series resistance, since a short-circuiting ignitron interposes an arc drop in the short circuit. The ignitron-tube method also provides a simple means of placing the arc-back at any desired point in the cycle.

I do not agree with the sampling method of testing in which all loading tests are omitted on all but a small percentage of a number of rectifiers of the same design. No physical or chemical tests have been established that will predict when an individual rectifier will arc back. I have found that a short duty-cycle type of commercial test applied to every unit is invaluable in maintaining quality of both workmanship and material in the factory.

I was happy to note that the authors favor the oscillograph method of measuring arc drop, as we have found this to be the only really satisfactory method. However, rather than the pool-type rectifier and battery, we favor the use of dry-plate rectifiers in both the oscillograph and by-pass circuits as described by W. B. Batten.[1] By the use of a not too sensitive oscillograph element a value of resistance can be used in series with the oscillograph which will pass about 0.1 ampere at arc-drop voltage and thus avoid the high forward-resistance of the Rectox rectifiers at extremely low currents. With this value of series resistance the reverse voltage of a 600-volt rectifier will pass about five amperes which requires a larger Rectox in the by-pass circuit. However, such a Rectox is not objectionably large for short-time duty.

The authors mention the building of rectifiers for an extremely low arc-back rate for critical applications with the necessary sacrifice in efficiency caused by higher arc drop. I question if this will be necessary or desirable, since with modern anode breakers an arc-back can be cleared by opening only the pole connected to the anode which arcs back, and this pole can be reclosed with no loss of service. I want to emphasize the authors' statement that rectifiers in general do not arc back with increasing frequency with time in service but rather settle down to a uniform rate. The recent extensive experience establishes this characteristic, at least with the ignitron type of rectifiers in service up to six years.

REFERENCE

1. OXIDE DISKS AID IN OBTAINING ARC-DROP WAVE, W. B. Batten. *Electric Journal*, April 1930, page 240.

Theory of Rectifier—D-C Motor Drive

Discussion and author's closure of paper 43-90 by E. H. Vedder and K. P. Puchlowski, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, pages 863–70.

D. E. Marshall (Westinghouse Electric and Manufacturing Company, Bloomfield, N. J.): The equations derived in this paper point the way to the use of a very convenient graphical analysis by which the wave form of the current can be obtained without the use of tedious calculation.

Equation 12 of the authors' paper can be written as

$$\frac{iZ}{E_m}=\left[\sin\ (x_f-\theta+\omega t)+C\epsilon^{-\frac{\omega t}{\tan\theta}}-\frac{a}{\cos\theta}\right]$$

where the symbols have the same meaning as given in the paper.

The sine term can be laid out lagging the voltage wave by the usual power-factor angle θ. The term $a/\cos\theta$ is laid out to the same scale as the sine term, and, since it is a constant, it can be added to the sine term as a shift in the zero line.

The exponential term can be laid out on a template to any convenient scale, the important thing being that it have a time constant equal to $\tan\theta$. Another convenient method is to draw the curves on tracing paper and superimpose through the transparency.

It should be noticed that it is not necessary to calculate the constant C, as the effective value of this constant can be changed by changing the origin of the time scale. This is due to the fact that

$$C\epsilon^{-\frac{\omega(t+t')}{\tan\theta}}=C\epsilon^{-\frac{\omega t}{\tan\theta}}\epsilon^{-\frac{\omega t'}{\tan\theta}}=C'\epsilon^{-\frac{\omega t'}{\tan\theta}}$$

A little thought will show that when the sine and constant term are added, their sum will not, in general, be zero at the time desired, that is, when the conduction starts. If the exponential template is based on the displaced zero line of the sum of the sine and constant term in such a manner that the curves intersect at the starting angle, they will again intersect at the stopping angle. The vertical distance between the two curves at each point from starting to stopping angle will generate a curve proportional to the current as a function of time.

Two examples are shown—Figure 1 for rectification and Figure 2 for regeneration (both of this discussion). Each curve is marked to indicate which portion of the

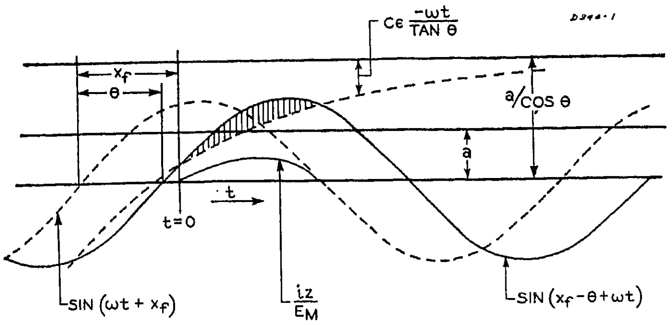


Figure 1. Graphical calculation — rectification

$a=0.6$
$\cos\theta=0.4$
$\theta=66.4$ degrees
$x_f=75$ degrees

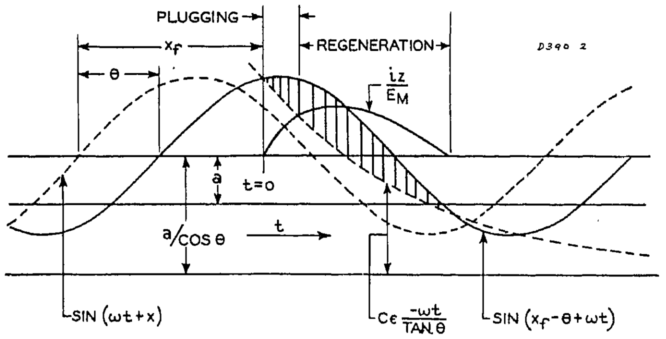


Figure 2. Graphical calculation — regeneration

$a=-0.6$
$\cos\theta=0.4$
$\theta=66.4$ degrees
$x_f=142.5$ degrees

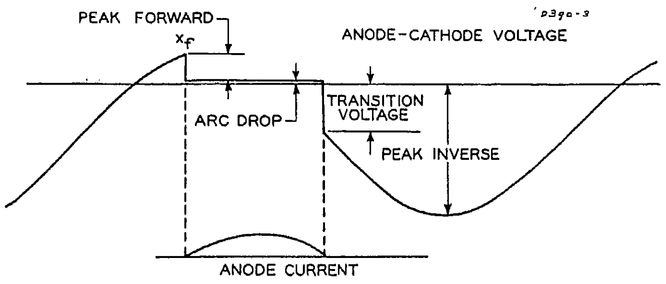


Figure 3. Tube loading—rectifier operation

$a=-0.6$
$\cos\theta=0.4$
$x_f=75$ degrees

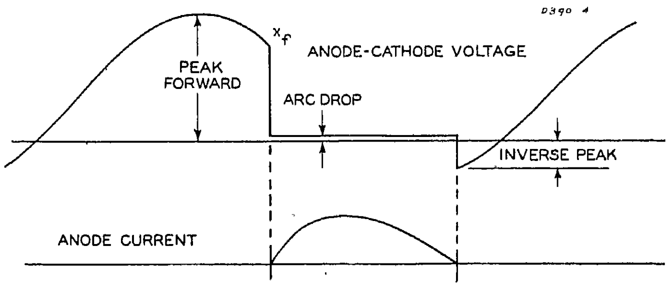


Figure 4. Tube loading — inverter operation

$a=-0.6$
$\cos\theta=0.4$
$x_f=142.5$ degrees

equation it represents. Note that in rectification the value of a is positive and in inversion it is negative, making the displaced zero line positive in one case and negative in the other case and thus inverting the exponential term in respect to the sine term.

Figure 3 and Figure 4 of this discussion show the duty on the main power tubes as anode current and anode-cathode voltage. Note that rectifier operation requires ability to withstand inverse voltage, and that inverter operation requires ability to withstand forward voltage.

The point of end of conduction in rectification stresses the tubes with suddenly applied voltage. The duty here depends on the magnitude of the suddenly applied transition voltage and the previous rate of decrease of anode current.

The end of conduction in inversion service should find the anode negative, as shown, with sufficient time elapsing to allow for sufficient deionization of the tube to enable it to regain control against the following forward voltage. This is controlled by the angle of firing the thyratrons. Earlier angles are more favorable to deionization but conversely result in a larger percentage of plugging versus regeneration.

It should also be noticed that the ratio of peak to average current is high and during starting periods may approach the peak rating of hot cathode tubes.

If inertia loads are accelerated, attention must be paid to the maximum averaging time rating of these tubes so that their heat capacity is not exceeded.

G. E. Walter (General Electric Company, Schenectady, N. Y.): One immediate reaction to this useful paper is that its title is rather inappropriate. The data, curves, and basic principles that it presents are not necessarily restricted to motor applications, but may be applied with equal facility to rectifier loads containing such sources of counter electromotive force as batteries or large capacitors. In this sense, the title is too narrow. It is too broad, however, in presuming to discuss the entire theory of rectifier–d-c motor drives from the initial assumption of discontinuous current flow.

Operation of a motor with continuous current delivered from a rectifier is frequently more normal and more important than running it with discontinuous current. Despite the implication of this paper that operation beyond the authors' calculations is not feasible, the point in an actual drive at which individual current pulses merge to produce continuous current is all but indistinguishable. Both the rectifier and motor make the transition smoothly. The important effect of having continuous current in the motor armature is that the familiar d-c motor equation can be used once more, since

$$V_t=(E_g+E_l)+I_a r_a \tag{1}$$

where V_t is the average voltage output of the rectifier unit, and I_a is the average value of armature current as indicated by a D'arsonval-type ammeter.

It is relatively simple to extend the material in this paper into the region of continuous current by using the expression for the average voltage output of the rectifier as

$$V_t=\sqrt{2}E_{\text{rms}}\frac{P}{\pi}\sin\frac{\pi}{P}\cos\delta \tag{2}$$

where

E_{rms} = voltage to neutral of the anode transformer in the rectifier unit
P = number of rectifier phases
δ = angle by which the firing of the rectifier tubes is delayed beyond the point at which firing would occur if there were no grid control

This angle of retard, δ, is not the firing angle, X_f, mentioned in the paper, except in the case of the biphase single-way (or half-wave) rectifier, identified by the authors as a single-phase full-wave rectifier.

If this relation (equation 2) for V_t is substituted in the motor equation 1, and both sides are divided by $\sqrt{2}E_{\text{rms}}$, the result may be expressed in the terminology of the paper as

$$\frac{P}{\pi}\sin\frac{\pi}{P}\cos\delta=\alpha+(\text{average torque factor}) \tag{3}$$

In the biphase single-way rectifier, on which the majority of their data are based, this equation may be reduced further to

$$\text{Average torque factor}=0.637\cos X_f-\alpha \tag{4}$$

when it is remembered that armature current must be continuous if this relation is to be valid.

Equation 4 shows directly the average output torque of the motor for all values of the speed factor, α, from zero to 0.637, which is the highest speed factor at which the motor will operate with continuous current. If α should exceed 0.637, as it can if the motor load is light, armature current will become discontinuous, and the average motor torque will remain positive.

The paper unfortunately minimizes the

importance of operation with continuous current by basing its characteristic curves on values of armature-circuit power-factor angle, Θ, that are not altogether typical. Measurements on a General Electric 5*B*204*A*56 1,150-rpm 230-volt one-horsepower shunt motor have indicated that the armature and commutating field in series have a resistance in the order of 6.6 ohms and a minimum inductance of about 0.05 henry. The power factor, $\cos\Theta$, is thus 0.33, definitely less than the 0.4 and 0.6 the authors have used. With the shunt field weakened for higher speed operation, armature-circuit inductance rose to about 0.07 henry, further reducing the power factor.

On another model, a General Electric 5*B*254*A*23 1,150-rpm 230-volt three-horsepower motor, armature circuit resistance is 1.85 ohms, and inductance is 0.019 henry, yielding a power factor of 0.251. If, as is possible, an external reactor is connected in series with the motor armature to improve commutation or to produce a more uniform torque, the power factor is extremely low. A reactor available for the one-horsepower motor already mentioned has an inductance varying from 0.2 to 0.6 henry and drops the power factor to the neighborhood of 0.01.

This means that discontinuous conduction will exist in a more narrow operating region than the typical curves in the paper would indicate. Specifically, Figures 5, 6, 7a, 7b, 10, and 11b of the paper are affected. Figure 4 of the paper can be used, with the proper power factor, to locate the boundary between continuous and discontinuous conduction.

One feature of Figure 4, however, was not mentioned by the authors. Although the curves from which X_s can be found as a function of X_f have no significance once they reach the line *BD* (or *B′D′* or *B″D″*) that corresponds to the type of rectifier used, the relation between the two angles can still be found by following the appropriate line *BD* (or *B′D′* or *B″D″*) downward as an extension of the particular curve that was used in arriving at the line. This follows from the obvious relation for continuous conduction that

$$X_s = X_f + \frac{2\pi}{P} \qquad (5)$$

Finally, the original assumptions, some of which the authors admit are inexact, can be examined to locate the approximations. Shunt machines are equipped normally with a series winding of some sort, which a casual observer of the assumptions might believe would constitute a disturbing influence on the operation of the drive. Actually, the action of a series field remains substantially the same whether the motor is operated from a rectifying or a rotating source of direct current. The field, moreover, need not be saturated for useful operation. Motor speed will be increased and shaft torque decreased if the field is weakened.

There have been indications that armature reaction is not negligible in motors operated from rectifiers because of the relatively high peak currents produced, but its effect is ordinarily undisturbing. Armature inductance, however, is not constant. It depends on the instantaneous values of armature and field currents; and for field-weakened operation of the motor, the inductance of the armature and commutating-field windings may be as much as 30 per cent or 40 per cent higher than during full-field operation.

K. P. Puchlowski: In his remark concerning the title of the paper, Mr. Walter has stated correctly that the mathematical analysis presented therein can be extended directly to apply to a much broader region of electrical phenomena. The motor drive, however, is one of the most prominent and immediately useful applications of the presented theory. The theory has been developed specifically and adapted for the rectifier–motor system. To that end, a number of concepts and mathematical coefficients have been worked out to make the analysis directly adaptable for that kind of system, and, consequently, the title of the paper was chosen to indicate this.

Mr. Walter seems to underestimate the importance of the range of discontinuous current in the motor armature. The question of whether the operation of a motor with continuous or discontinuous current is more advantageous may be, of course, the subject of a separate discussion. It should be remembered, however, that the determining factor for the continuity or discontinuity of current is the design of the system and not its normal or inherent properties, as Mr. Walter seems to imply. In fact, any system can be designed in such a way that a discontinuous current will flow in the armature over the entire range of loads and speeds, regardless of how large the inductance of the circuit might be. On the other hand, in any workable system the current will usually pass through the discontinuous stage at low loads even if at higher loads it becomes continuous. With this importance of the range of discontinuous current in mind, as well as the fact that this range had not been explored sufficiently mathematically, a new method of analysis has been developed and presented in this paper.

Mr. Walter's statement, that the well-known d-c motor equation 1 of his discussion can be used only in the case of continuous current, does not seem to be justified. In fact, the equation

$$V_t = (E_g + E_l) + I_a r_a$$

is valid also in the case of a discontinuous current with a sole correction that V_t is not the average rectifier output voltage but the voltage across the armature of the motor. It should be emphasized that only for continuous current the rectifier output voltage is equal to the armature voltage of the motor, whereas in the region of discontinuous current these two quantities are entirely different ones. Thus, if the definition of the symbol V_t is changed to represent the armature voltage, the validity of equation 1 is extended over the entire range of current, both continuous and discontinuous.

The general equation 2, presented by Mr. Walter, is valid only in conjunction with certain important limitations which, unfortunately, have not been clarified. To be valid from the electrical point of view the equation 2 must fulfill the following condition:

$$V_t > 0 \text{ which means } \cos\delta > 0$$

Taking into account that in any case $0<\delta<\pi$, we finally get $0<\delta<(\pi/2)$.

It can be seen readily that the preceding limitation is the necessary, though not the only, condition for obtaining continuous current in the armature. In other words, if the aforementioned condition is met, continuous current may be obtained, provided the power factor of the load is sufficiently low. On the other hand, if $\delta>(\pi/2)$, the current always will be discontinuous, regardless of how low the power factor of the load might be.

The values of power factors selected for some curves in the paper have been chosen with the primary view of results which can be obtained from the presented theory and as such, of course, retain their full validity, even if some are a little too high in comparison with the values of power factor for the two particular motors which Mr. Walter has selected. Westinghouse Company experience would indicate that the typical power factor is higher.

The purpose of this paper is to give a new approach to the theory of a rectifier–motor drive without going into the problem of particular designs and constants of the system. Hence, it seems beside the point to discuss details which, in any case, cannot be covered fully in a single paper.

Finally, some of the original assumptions apparently have been misinterpreted by the author of discussion. Assumptions 1 and 10 of the paper were made merely to emphasize that the main flux in the motor is constant, which again was necessary for the assumption 6. The assumption 2 was brought about in order to back the assumption 3.

These assumptions, of course, do not represent a set of conditions necessary for proper operation, but they were assembled simply to make a clearer picture of the presented theory and especially to simplify the mathematical approach and the interpretation of certain parameters appearing in the equations.

The very interesting graphical method of determining the wave form of the current pulse, given in Mr. Marshall's discussion, may prove very helpful and also is a valuable addition to the basic chart shown in Figure 4 of the paper. Mr. Marshall's remarks and illustrations of the forward-peak and inverse-peak voltages for both rectifying and inverting operations of the tubes, though not directly connected with the paper, represent an information valuable to anyone concerned with the operation of thyratron tubes.

Thyratron Motor Control

Discussion of paper 43-116 by E. E. Moyer and H. L. Palmer, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 706–12.

E. H. Vedder (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The development described in this paper is an important one, and the presentation is timely. The electronic circuits are described thoroughly, and reasons are given for the various functions and features.

Greater use of supporting technical data could have made the paper more valuable to the profession. For instance, the curves in Figure 5A of the paper are limited apparently to the armature control range with no information on the regulation with field weakening. Some of the type of data I have in mind were included by one of the authors of this paper and printed in a trade magazine. Also, the type of information used by Mr. Moyer in his presentation would have been useful in the formal paper.

The description in the paper leaves the impression that the complete circuits shown are used for all applications. A short analysis of applications would have clarified this point. Many applications do not need extremely precise control, and such features as regulated standard voltages and armature-voltage limit, when decelerating, may not be required. For instance, it would seem unnecessary to limit armature decelerating voltage on a 230-volt motor, unless the field-weakening range is more than 2:1. The use of regulated standard voltages is not necessary for most applications and is required only when very precise speed regulation is desired.

Many users are quite satisfied with the characteristics of various adjustable speed drives using rotating machines but will change to the electronic drive if it is not too complicated and has advantages of limiting noise, vibration, and mounting problems. A large portion of jobs will be solved with only a portion of the complication of the complete circuit shown. It is my hope that such a basically good scheme will be kept as simple as possible, so that it will be accepted rapidly.

K. P. Puchlowski (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The presentation of the paper was very interesting. Unfortunately, none of the valuable information presented has been included in the paper, which gives only a description of a particular electronic circuit providing adequate control of a d-c motor driven from a controllable rectifier. The paper would have been improved, however, by the inclusion of some information regarding special phenomena and operating conditions which characterize a rectifier-motor system.

Considerable emphasis is put on the fact that a constant reference voltage, independent of line-voltage variations, is obtained and is used for regulating purposes. Since no further explanation of what these regulating purposes actually are is forthcoming, it may seem that the reason for a complicated network constituting a feedback from the armature voltage and consisting of electronic tubes *D* and *C* is solely for the purpose of maintaining the armature voltage independent of line-voltage variations. Consequently, many may ask whether, in the case where changes in speed caused by normal line-voltage variations are acceptable (and this is true for most industrial applications), the feed-back circuit cannot be eliminated, and a direct control of grid-voltage phase shift is used instead, resulting in a basic simplification of the circuit.

The answer is that the feedback from armature voltage is needed for something else, namely, to prevent the voltage across the brushes from dropping rapidly with increasing torque applied to the shaft of the motor, a phenomenon which is unknown in conventional d-c motor practice.

With reference to Figure 5A of the paper, it is stated that the dotted curves represent the normal uncompensated motor-regulation characteristics. It is not known, however, whether these characteristics refer to a conventional d-c drive, to an uncompensated rectifier drive with regulated armature voltage, or to an uncompensated and unregulated rectifier drive. In that way, from the engineering point of view, the situation is not clear.

The regulation curves shown in Figure 5A terminate abruptly at about 145 per cent load, and the vertical line which follows thereafter seems to indicate the current or torque limit. This seems to be in line with the statement that the speed will be maintained up to the point where the current reaches the current-limit setting. However, the form of these regulation curves, although very desirable, would seem rather unusual, on account of that sharp break, as the 145 per cent value of load is reached. The speed would probably decrease gradually as the current limiting tube *E* comes into action.

The necessity of a feedback from field voltage to make it independent of line-voltage variations can be questioned also. Even if the field voltage is regulated and maintained constant for a given setting of the speed-control potentiometer, the field current will vary with temperature of the field winding, and, in addition to that, the flux will vary with load on account of armature reaction. In that way, it is apparent that the field voltage is not an adequate indication of the magnetic flux, and, it would seem, therefore, that a much simpler scheme of field control by direct phase displacement of the grid voltage of field thyratron tubes would be justified fully.

Load Pickup by a Group of Ignitron Rectifiers

Discussion and author's closure of paper 43-115 by L. W. Morton and D. I. Bohn, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 679–84.

J. H. Cox (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Morton and Bohn have reported the results of both an interesting and a valuable series of tests that disclosed the basic starting up and dropping characteristics of aluminum pot lines with mercury-arc rectifiers. With this information, schemes can be applied with much greater assurance than has been possible in the past. They have also described a perfect method of master starting which is not prohibitively complicated. The method does add some complication and additional apparatus over the method of master excitation, and any impression that all systems of master excitation have proved unsatisfactory is not justified.

The tests showed that, with most systems of master excitation applied with contactors having entirely normal and practical adjustments, no breaker or rectifier was subjected to more than a few per cent over its continuous load rating because of the existing pot-line inductance. Therefore, if the rectifier breakers are free of rate-of-rise influence in the forward direction, all rectifiers should stay on in this method of pot-line starting. Even the tripping of one or two rectifiers in starting should not be considered too serious, since ignitron rectifiers have ample short-time overload capacity to enable the pot line to stick, and the rectifiers that tripped can be returned to the bus in a leisurely manner.

Also, I believe that in order to provide reliable excitation under other abnormal conditions which are encountered, an excitation system must have sufficient output energy to provide reasonably satisfactory excitation with cold ignitors. In view of these factors there seems some question whether the advantages justify even the relatively nominal addition of complication required by the master blocking system over the master excitation system.

Where phase-shifting networks are used instead of mechanical phase shifters, the transients are so long that a simple procedure of simultaneously energizing the circuits is not permissible. In this case the master excitation system must take some form of releasing excitation, such as removing short circuits around the ignitors.

C. C. Herskind (General Electric Company, Schenectady, N. Y.): The starting method described in this paper is based on the ability to block the firing of an ignitron rectifier by the application of a negative voltage to the grid.

The primary function of the grid (or anode baffle, as it is frequently called) in the ignitron rectifier is to deionize the space around the anode. The grid is designed to do this with a minimum increase in arc loss. Structurally, it consists of a graphite basket surrounding the anode and having a wall of approximately one-inch thickness with three-fourths-inch or one-inch holes. These large holes and the arrangement in the tank limit the ability of the grid to control the firing of the anodes.

The blocking characteristics of the grid in a commercial ignitron rectifier are shown in Figure 1 of this discussion. These characteristics were determined by applying a positive voltage to the anode and a negative voltage to the grid with the ignitor firing. With sufficient negative voltage on the grid,

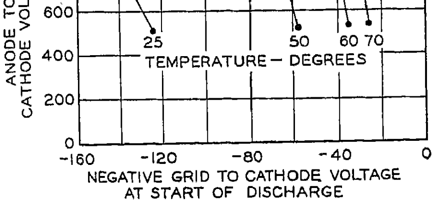


Figure 1. Grid-blocking characteristic of ignitron rectifier

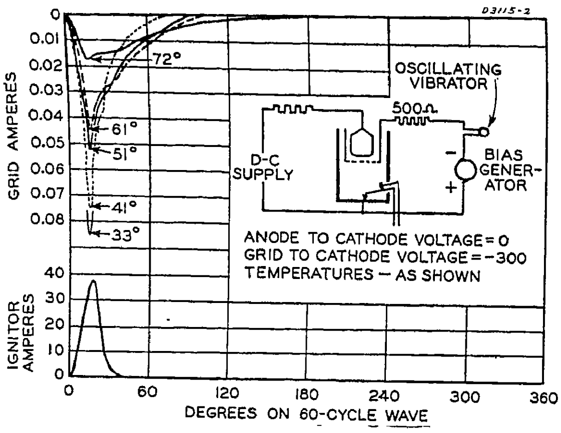


Figure 2. Grid-current wave shape during blocking test

the anode is prevented from firing. By gradually decreasing the negative voltage on the grid, a point is reached at which the blocking action fails, and the anode fires.

These curves show that a greater negative voltage is required on the grid at the lower temperatures in order to block. This is due to the larger amount of ionization reaching the anode at the lower temperatures. The grid-current wave shape under blocking conditions is shown in Figure 2 of this discussion. It may be noted that more grid current is obtained at the lower temperatures.

The slow deionization of the arc space in an ignitron rectifier is also shown by these grid-current wave shapes. At 72 degrees centigrade the grid current persists for over 120 degrees after the ignitor-current wave. At the lower temperatures the decay of ionization takes place more rapidly.

Such a slow deionization of the arc space renders it incapable of controlling the firing of the anode under load, that is, phase control. In the usual ignitron rectifier, phase-control operation is obtained when the firing of the ignitor is controlled. However, it is possible to provide phase control by means of the grids, the same as in a multianode rectifier, when a suitable grid and baffle arrangement are provided.

H. Winograd (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): In March 1939, the writer of this discussion made tests at the Alcoa plant of the Aluminum Company of America on switching of an aluminum pot-line circuit supplied by rectifiers with continuous excitation and control grids. These tests were made in co-operation with D. I. Bohn, coauthor of this paper. The writer thought it would be of interest to describe these tests and their background, in view of their similarity to the tests described in the paper.

In the first rectifier station for an aluminum reduction plant, which was put into operation at Alcoa in 1938, a 50,000-ampere d-c breaker was used for switching the pot-line circuit. This followed the practice formerly used in rotary converter stations. The cost of the breaker and its installation were a substantial item. The d-c busses were located outdoors, and the 50,000-ampere circuit had to be brought into the station to the breaker and out again. The maintenance of the breaker was also a problem. Since it remained in the closed position for months at a time, the closing and tripping mechanisms became sluggish and sometimes failed to function properly.

In view of these objections, other methods of switching the circuit were explored. As the control grids of the rectifiers can control the pickup of the anodes and can keep them blocked, it was decided to utilize this property for switching the load circuit.

Each pot-line circuit was supplied by ten rectifiers operating in parallel. The units were displaced in phase from each other by means of wye and delta transformer primary connection and phase shifters ahead of the transformers to constitute a 30-phase system. The grids of each rectifier were controlled by means of an a-c voltage from a six-phase grid transformer and a variable d-c bias connected between the neutral of the grid transformer and the cathode of the rectifier. When the bias is negative, the a-c component of the grid voltage causes an anode to fire at a point in the cycle where the net grid-to-cathode voltage changes from negative to positive. With the grid transformer de-energized, the anodes remain blocked.

For testing the pickup of the pot-line load, the primary windings of the grid transformers of all the rectifiers were connected to a common three-phase supply through a three-pole contactor. The grid bias on the rectifiers was set at about 50 volts negative. With the load off, the grid contactor was opened. The pot-line breaker was closed, the rectifiers were connected to the a-c supply and the d-c bus, and the excitation arcs were established. The power circuits were, therefore, complete, and the rectifiers were ready to take load but were kept blocked by the negative voltage on the grids. The grid-supply contactor was then closed, and the rectifiers picked up the load, as shown in the oscillogram of Figure 3 of this discussion. The top trace of this oscillogram shows the current of the pot-line circuit. The other traces show the currents of five individual rectifiers. This test was repeated several times with the same results. This solved the problem of switching on the load.

For dropping the load, two methods were considered and tested. One method was to block the rectifiers by means of the control grids. The second method was to gang-trip the cathode breakers of the rectifiers.

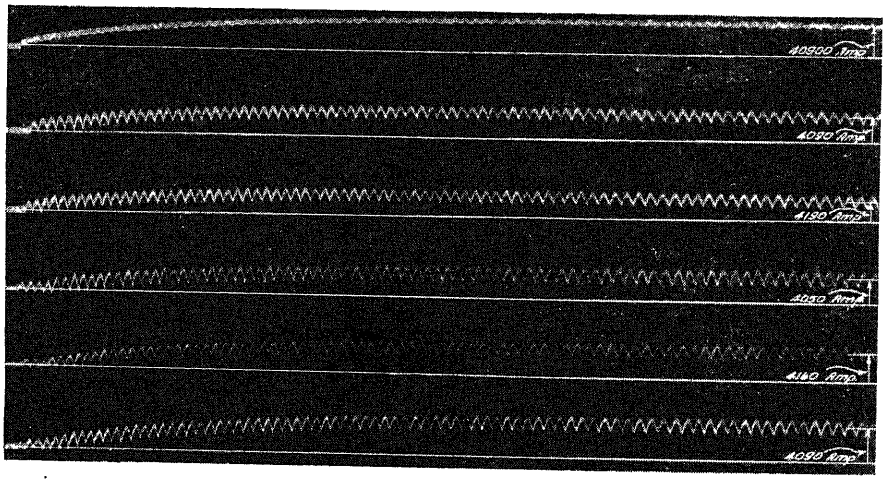
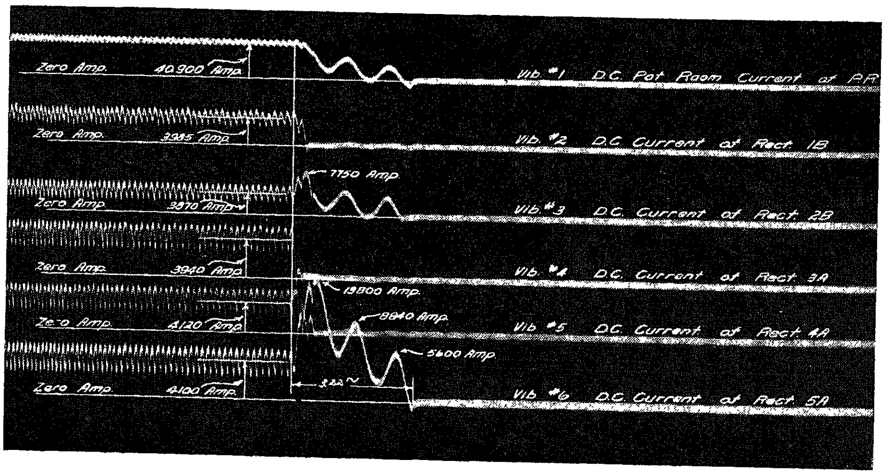


Figure 3 (above)

Figure 4 (below)



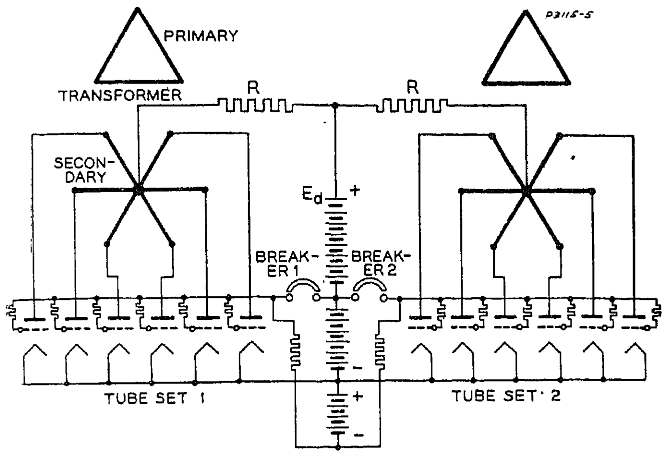


Figure 5

The grid-blocking method was tested by opening the contactor supplying the grid transformers, which left a negative bias on the grids of all the rectifiers. The results are shown in the oscillogram of Figure 4 of this discussion. The explanation of the oscillogram is as follows:

When the a-c grid supply was interrupted, applying blocking potential to the grids, the anodes which were not firing remained blocked. Some of the anodes which were firing dropped their current at the end of their firing period and remained blocked, the load current being transferred to other firing anodes in the same rectifier or another rectifier. The transfer of current between the rectifiers was facilitated by the fact that the rectifier units are displaced from each other in the 30-phase system. The result was that the anodes connected to one phase in several rectifier units continued to carry the current, because the energy stored in the inductance of the pot-line circuit maintained the flow of current over the entire cycle of the a-c voltage, until the energy was dissipated. This accounts for the 60-cycle a-c components in the pot-room and rectifier currents. The test was repeated several times, with similar results. The ripples in the current waves, prior to dropping the load, are exaggerated considerably by induction in the connections between the ammeter shunts and the oscillograph.

The effect of the pot-line inductance on the currents of the rectifiers was anticipated when this method of dropping the load was considered. However, it was decided to make the test. The results were somewhat better than expected, as it was expected that only one rectifier unit would carry the load until the energy of the pot-line inductance was dissipated. This method of dropping the load was considered unsatisfactory, on account of the overloading for several cycles of one rectifier-transformer phase and the anodes connected to it. It was therefore decided to drop the load by gang-tripping of the cathode breakers, which was tested and found satisfactory.

The method of switching the pot-line load described previously was applied in the next rectifier station at Alcoa, which was put in operation in May 1940, and in subsequent installations. It was also adopted in the first rectifier station, and the pot-line breakers were removed. The switching operations for picking up and dropping the load are performed by one control switch. When turned to the close position, the control switch energizes the three-pole contactor supplying the grid transformers. When turned to the trip position, it trips the d-c bus-tie breakers to the adjacent pot lines and operates a multi-pole relay for gang-tripping of the cathode breakers.

In the oscillogram of Figure 6 in the paper, showing the gang-tripping of the cathode breakers, there is a considerable time difference in the opening of the cathode breakers. This caused overloading of several rectifiers, the current on rectifier $5B$ attaining a value of 27,800 amperes. Such overloading is undesirable and is likely to cause arc-backs. This could be avoided if the contacts of the gang-tripping contactor and the shunt trips of the breakers were adjusted to get simultaneous opening of all the breakers. Such adjustments were made in the installations with which the writer was connected. A fractional-cycle electronic timer, connected as shown in Figure 5 of this discussion, was used for this purpose. It was determined by means of an oscillograph that the mechanical opening time of the cathode breaker was about $2^1/_2$ cycles from the instant the shunt-trip coil was energized. It was also found that the difference in the opening time of individual breakers was less than one cycle, if the air gaps of the shunt-trip armatures were approximately the same.

The timer consists of two tube sets. Each set consists of six thyratron tubes connected to the six-phase secondary winding of a small transformer. A source of d-c voltage, shown in the figure as a battery, is connected between the cathode of the tubes and the neutral point of the transformer in series with a current-limiting resistance R. The voltage E_d of the d-c source, measured between the positive terminal and the cathode connection of the tubes, is higher than the peak value of the phase-to-neutral voltage of the transformer, so that if only one phase were firing, the current through the tube will remain above zero continuously.

The contacts of the breaker are connected to the grid circuit of the tube set so that, when the breaker is closed, the grids are at a positive potential to the cathode, and when it is opened they are made negative. When the breaker is closed and the grids are positive, the thyratron tubes fire in sequence for one-sixth-cycle periods, the commutation being effected by the transformer secondary voltages. When the breaker is opened and the grids of the tubes are made negative, the tubes which were not firing at that instant remain blocked. However, the tube which was firing continues to fire, as its current does not reach zero, and its grid is ineffective. The phase position of the tube which remains lit indicates the part of the cycle when the breaker contacts opened.

When two breakers, which are to be compared, are connected to the two tube sets and are tripped by means of the gang-tripping switch, the difference in the phase position of the lit tubes will indicate the difference in the opening time of the breakers. The backstop of the shunt-trip armature of one breaker is then adjusted until their opening time is the same, as indicated by the tubes. The breaker adjustment was considered satisfactory if the corresponding tubes in the two sets were lit in three out of four trials. One breaker was used as a reference breaker, and each of the other breakers was adjusted in turn to agree with it.

REFERENCE

1. ALCOA RECTIFIER INSTALLATION. J. E. Housley, H. Winograd. AIEE TRANSACTIONS, volume 60, 1941, pages 1266–74.

L. W. Morton: The authors agree with Mr. Cox that, provided the excitation equipment has sufficient energy to fire cold ignitors, the master excitation system can be operated successfully. Under the circumstances, while the grid-blocking method causes perfect pick up, the additional complication of grid blocking is not justified where the rectifiers are equipped with up-to-date excitation circuits, all of which have sufficient output energy to fire cold ignitors.

It should be kept in mind, however, that before the tests were made, the cause of improper pick up and over-all behavior of the equipment during starting up and dropping was not fully understood. Use of grid blocking provides perfect starting for the earliest group of 12 ignitron rectifiers placed in Aluminum Company service. The alternative of replacing 12 excitation circuits with higher-voltage circuits would have been too costly in critical materials to be justified at that time.

By means of the tests the authors were able to solve their immediate problems. If, as Mr. Cox says, "With this information schemes can be applied with much greater assurance than has been possible in the past," the authors' efforts have been worth while.

Mr. Herskind and Mr. Winograd have added valuable supplementary data pertaining to the application, installation, and operation of rectifiers for cell-line service. Mr. Winograd's suggestions for adjusting the opening time of cathode breakers, to facilitate disconnecting cell lines, are particularly interesting and should be of benefit to operators of large rectifier installations.

High-Voltage Power-Transformer Design

Discussion and author's closure of paper 43-88 by M. B. Mallett, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–24, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 526–31.

Hamilton Treadway (Rural Electrification Administration, St. Louis, Mo.): This paper is a timely contribution in the field of transformer design, since it comes during a period when the need for innovation in the design of transformers is most pressing if a reduction in the materials required in their manufacture is to be effected. The writer has been studying this problem for several years with the object of reducing the weight and cost of the transformer while at the same time improving its characteristics. The design features discussed by the author are of particular interest in this connection, and the opinion of the author on several points is desired.

The temperature at which oil deteriorates has long been recognized as determining the capacity limits of the apparatus. The limiting temperature imposed by oil is quite low. Yet, as the operating temperature is increased, over-all weight per kilovolt-ampere of capacity is decreased. As a solution to this problem, the writer has in mind the use of compressed gas as a cooling medium.

Compressed gas offers many advantages, not the least of which is its high dielectric strength. Coupled with the use of class-*C* insulating materials, the insulation level of the equipment can be increased greatly. Furthermore, it does not deteriorate when subjected to high temperatures, and, in combination with mica, fiber glass, porcelain, and other solid materials not affected adversely by heat, the operating temperature of the transformer may be increased considerably.

Compressed gas provides a superior cooling medium because of the ease with which it can be circulated. If the transformer is operated well above 100 degrees centigrade water can be used to cool the gas, and thus advantage may be taken of its great latent heat and low cost in most localities.

The author's opinion on the adaptability of the distributed concentric construction to the use of compressed gas as a cooling medium is desired. The writer has in mind the substitution of mica for the foundation cylinder with fiber glass used to provide the channel spacer. Porcelain would be used to provide spacing between coils, and mica and fiber glass or mica would provide the turn insulation. This construction should facilitate greatly the circulation of the gas and should permit greatly increased heat flow across each unit of surface between the heated parts and the circulating medium, since the outer surface of the coils will be exposed directly to the flow of the gas.

The reduced reactance of the distributed concentric construction offers a further advantage. The upper limit of the operating temperature of transformers, deliberately designed for high-temperature operation, would be fixed by the limits to which the resistance of the winding may be increased and still maintain satisfactory regulation characteristics. A design which decreases the inherent reactances increases this limit.

The extension of service into rural areas has demonstrated a need for improved transformer designs combining lower losses, higher insulation levels, greater capacity per unit of weight, and lower cost. Whatever direction is taken in the further development of the rural-distribution and substation transformers, the finished product must provide these essential features in its design.

E. V. Leipoldt (Shawinigan Water and Power Company, Montreal, Que., Canada): There are several features in the design and construction of the distributed concentric type of transformer, described by the author, which are of special interest to operating engineers. The method of shielding, and, particularly, the structural support of the line end shield are to be commended. The fact, that each coil, or group of coils, is rigidly supported on hard-walled cylinders without the aid of an impregnating varnish, is an advantage which the maintenance engineers will appreciate.

The most appealing feature in the general design is, to my mind, the adaptability of this construction to forced circulation of oil through the windings.

The Shawinigan Water and Power Company for many years has advocated the application of forced-oil circulation through the windings of the transformers as well as in the external coolers. Unfortunately, our appeals to the transformer designers have not heretofore been taken very seriously, and it has required the combined pressure of a conservation program and increasing technical interest in the subject to bring about these worth-while economies

Our company took the first step in this direction in 1936 by the purchase of two 50,000-kva three-phase transformers with external forced-oil and air-cooled heat exchangers. These transformers were of the shell type, and the first stage in directed flow through the windings was taken by enclosing the base of the core structure and leading the return oil into the enclosed area below the windings. This arrangement was made with the reluctant approval of the manufacturers and has been operating successfully to date.

No real economy was anticipated from this installation, as the oil circulated through the winding was governed by the requirements of the external cooler, in this case, 450 imperial gallons per minute. A subsequent installation of two similar transformers was made in 1939–40, and steps were taken to provide not only excess cooling capacity in the heat exchangers in view of possible increased loading of the transformers, but also additional oil-circulation facilities to regulate the flow of oil through the windings of the transformer. Unfortunately, the war in which our country has been engaged since installation of the new transformers has prevented a program of testing which has been under consideration.

New transformer capacity has been added recently to this substation, and we hope to carry out some tests to determine the extent of increased loading with increased circulation of oil.

D. B. Fleming (The Hydro-Electric Power Commission of Ontario, Toronto, Ont., Canada): The paper presented by Mr. Mallett covers a transformer-winding design which tends toward efficient cooling, simplicity in the proper distribution of voltage stresses, and ease in maintenance and repairs, combined with adequate mechanical strength. Naturally, the simpler one can design and build a transformer and retain the maximum of good qualities, the more desirable the product becomes.

Our operating experience confirms the author's statements in regard to the mechanical weakness of the conventional barrel-type cylindrical coil. The turns of flat copper wound on a vertical cylinder often telescope on each other under short-circuit conditions. The author appears to have overcome this possibility by enveloping each turn in an insulating channel, which provides a double layer of insulation to act as a mechanical support over the whole area of the under and top side of the turn and distributes the axial stresses evenly. It occurs to me that the column could be made even more solid and secure if the parallel conductors were wound on edge so that the wider surface would distribute the axial stresses better, and the transfer of heat to the oil would be mostly through copper, rather than layers of copper and insulation. The change in symmetry would not then be necessary at the ends where additional turn insulation is provided. This, however, might not be practicable for mechanical reasons.

It has been found that, in the disk-coil radial-spacer type of construction, because of the concentration of stresses at the spacers, there is a tendency for the edges to cut through. This is true especially at the end coils, where the insulation is reinforced. Furthermore, the spacers become embedded in the insulation, so that the intervening spaces between supports become partially, and sometimes wholly, blocked with resultant heating and deterioration of the insulation and, finally, failure. Similar types of failures have occurred on shell-type transformers, where the unit stress on the insulating spacers at the inside corners is increased because of lack of room for providing both adequate supports and oil ducts for cooling. The insulation crushes, allowing the turns to become displaced and short-circuited. Experience during operation will show whether or not this new construction the author describes will overcome these troubles.

In Figure 4 of the paper it may be noted that the ends of the cylinders are cut back, so that the axial support comes on the copper column and not on the cylinders. This is important and is often not appreciated by the workmen in the factory assembly. Inspection of transformers during factory construction often shows that careless assembly or lack of attention to small details of this kind vitiate a meticulous design.

It is not clear from the description or illustrations how an even distribution of pressure is effected by the end supports and clamps. Where the number of stacks of coils increases, the problem of supporting them evenly also increases. In the construction using a disk-coil primary winding and a cylindrical secondary winding, where there is vastly more insulation in one than the other, more shrinkage will occur in the former, and separate pressure blocks with accompanying means of adjustment are essential. The practice of rolling or precompressing the insulation, however, may overcome much of this shrinkage. Periodic inspection and maintenance have disclosed that as much as an inch thickness of insulating spacers must be added at the ends of the disk coils to tighten down the whole assembly securely on the clamps. In the shell-type transformer, the insulation shrinks, and the spacing strips often fall to the bottom of the tank, requiring major and costly repairs.

It may be noted that the tap leads are all carried down the face of the coil to the bottom, but the illustrations do not show where these are terminated nor the location of the ratio adjuster. There will be a considerable voltage difference between the tap lead and the bottom of the coil which introduces hazards. This might be overcome to a great extent by carrying the top taps out the top of the winding and the bottom taps out the bottom.

Simplicity of repairs of a faulty winding is an important feature of this type of construction, although the cost of a whole cylindrical coil may be considerably more than that of a couple of disk-type coils. Happily, however, the percentage of failures in the modern transformer is rare compared with the failures experienced in the older designs of 15 years ago and earlier.

The very effective cooling, by means of the straight vertical oil ducts, is a very important feature. It tends toward smaller and lighter transformers for the same kilovolt-ampere capacity. The Hydro-Electric Power Commission of Ontario is extending its practice of operating transformers on a thermal-capacity basis and adding additional cooling by means of fans to normal self-cooled units. The construction the author describes in his paper lends itself to forced-oil cooling. This is very interesting, as it permits the transformer to be purchased as a nominally rated self-cooled unit with certain provisions on it, so that, as the load grows, fans or blowers can be added and, finally, forced-oil cooling to obtain the desired increases in capacity.

H. H. Wagner (Pennsylvania Transformer Company, Pittsburgh, Pa.): There is a present tendency toward the use of cylindrical layer windings in power transformers, as indicated by this paper and other recent transformer technical articles.

For the past 15 years our company has built concentric cylindrical layer windings for voltages up to 66 kv and in sizes up to 20,000 kva. The design principles of this type of winding have been developed with the aid of a large amount of laboratory research, including 60-cycle and impulse tests and mechanical-pressure tests in our 300-ton hydraulic press.

This experience has indicated the applicability of this type of design, not only to the high-voltage ratings described in the paper, but also to medium- and lower-voltage classes.

Both our service record and laboratory testing confirm, in general, the conclusions of the paper as to the desirable electrical, thermal, and mechanical characteristics of this type of winding.

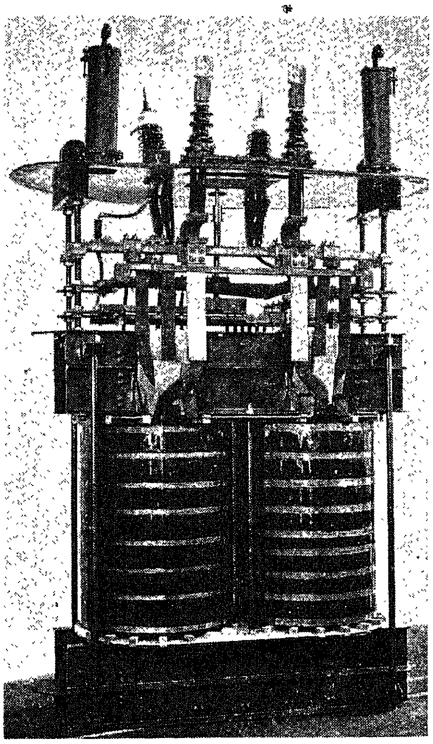


Figure 1. 5,000-kva core-type layer-winding transformer

This type of winding is also applicable to fully insulated transformers as well as to graded insulation. With full insulation it is desirable in the higher-voltage classes to use a shield at both line ends of the winding.

In the lower-voltage classes we have found that a good internal-impulse voltage distribution can be obtained without the use of shields. This is partially due to the higher layer to layer capacitance resulting from the use of thinner-layer insulation for the lower voltages.

Our experience has shown that this type of winding, together with a baffle as described for forced-oil cooling, also gives excellent results for forced-air cooling on dry-type air-cooled transformers.

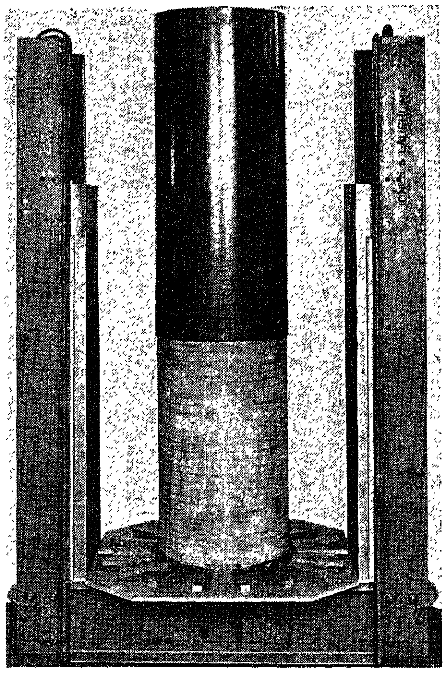


Figure 2. Double-magnetic circuit core—partially assembled

Our practice has been to use square or nearly square conductors for layer-type windings instead of the thin flat strap conductors shown in the paper. Of course, the use of square wire requires a larger number of conductors for a given total cross section, since the permissible thickness of the conductors is limited by the copper eddy-current losses.

A reliable mechanical construction is assured, by supporting each conductor on all four sides radially and axially, by the use of vertical spacers in close contact with each layer of conductors instead of supporting only three sides of each conductor as shown in the paper, and by thoroughly impregnating each winding in varnish.

The construction of a cylindrical-layer winding-core-type transformer is illustrated in Figure 1 of this discussion which shows the core and coil assembly of a single-phase transformer rated 5,000 kva, 34,500 volts.

While the core-type single magnetic circuit is suitable for many ratings using layer windings, wide use has been made of a double-magnetic-circuit-core design which has several important mechanical advantages wherever applicable. Figure 2 of this discussion shows a partially assembled core of this type, which illustrates also the method of supporting the windings.

M. B. Mallett: The author thanks contributors of discussion for their comments and interest.

Mr. Leipoldt's present advocacy of directed forced flow, after his seven years of actual experience with this type of cooling in large transformers, indicates to the author that directed flow in such machines must be entirely practical from an operating point of view.

Mr. Fleming suggests the desirability of not splitting the coil conductor radially. This is often feasible in 25-cycle designs, where the reduced frequency ordinarily permits some $^5/_{16}$-inch radial thickness of solid copper without excessive eddy loss. In connection with this point, the author emphasizes the fact that the basic requirements for barrel coil stability, as stated in the paper, are considered essential, irrespective of the form of coil conductor used, whether flat wound strands, edge wound strands, square strands, a single strand of any proportions, or a single cable comprising a number of continuously transposed strands. Because of their importance, these basic requirements for coil stability are reiterated here as follows:

1. Positive axial separation of conductors.
2. Firm coil clamping.
3. Freedom of coil-diameter expansion.

In regard to clamping of the final structure, the required pressure distribution on the various high-voltage and low-voltage coils is obtained by means of an adjustable plate located between the end insulation and fixed top clamp member. After final vacuum shrinkage, this plate is backed off, necessary adjustments are made readily in the various progressively disposed radial end spacers, and pressure is reapplied with the adjustable plate.

With reference to tap-lead arrangement, all straps are usually brought to one end of the tap coil to minimize the potential from leads to adjacent outer coil. Such an arrangement increases the potential difference between straps and the tap coil itself, but ordinarily this difference is relatively small, being approximately $7^1/_2$ per cent, for example, in the particular design illustrated by Figure 5 of the paper.

Mr. Wagner indicates the opinion that conventional-type barrel-coil construction is suitable for application in large power transformers. This opinion, however, conflicts with that of the author for specific reasons stated in the paper.

Mr. Treadway asks about the adaptability of the distributed concentric construction to the use of high-pressure gas in combination with class-*C* insulation. The general construction is uniquely suited for forced gas cooling, but the author believes that at present there are two basic obstacles to the practical application of compressed gas and extreme operating temperatures.

1. The erratic behavior of creepage surfaces under gas pressure in respect to impulse dielectric strength, as indicated by present knowledge of the subject.

2. The mechanical limitations of class-*C* materials as are now available.

Interim Report on Overloading Current-Limiting Reactors

Discussion of paper 43-109 by the transformer subcommittee of the AIEE committee on electrical machinery, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 611–12.

A. F. Phillips (Duquesne Light Company, Pittsburgh, Pa.): The Interim Report on Overloading Current-Limiting Reactors is a very good guide for usual conditions experienced in service. However, rather than attempt to follow any general rule in loading equipment, it is believed that the urgency of wartime needs amply justifies the effort required to place individual ratings on equipment. In most cases, a general method of rating leads to a conservative loading practice. However, there may be instances when this is not true. For example, we have some four-kilovolt dry-type reactors enclosed in concrete compartments with Transite doors. When full load is carried, the temperature rise above the ambient temperature was 79 degrees centigrade, whereas the rise in open air was only 39 degrees centigrade. This condition was corrected by providing adequate ventilation for the compartment, which reduced the compartment rise over ambient temperature from 40 degrees centigrade to 9 degrees centigrade.

In another case when the loading was increased above the name-plate rating of the reactor, considerable heat developed in the steel compartments. Currents up to 420 amperes were measured in the supporting angles for the compartment. The cubicle was modified by cutting both the upper and lower angles in the middle, breaking up the path for the circulating current. This reduced the compartment rise to a satisfactory value.

Interim Report on Overloading Distribution Transformers

Discussion of paper 43-87 by the transformer subcommittee of the AIEE committee on electrical machinery, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 516–17.

E. F. Dissmeyer (Commonwealth and Southern Corporation, Jackson, Mich.): The Interim Report on Overloading Distribution Transformers is somewhat disappointing in that it does not present any specific data. It merely states: "It is difficult to fix specific limitations for operation of distribution transformers," and "It seems necessary therefore that those users seeking the near ultimate in loading of distribution transformers base their thoughts and conclusions on the particular characteristics of the equipment and loads peculiar to their own systems." The reference list which has been provided will undoubtedly be appreciated by the industry.

The transformer subcommittee has made many fine contributions to the industry, and their possible failure to provide complete and usable data on the loading of distribution transformers might seem to indicate that no problem exists. It would seem, however, that the subcommittee could have made greater use of surveys of distribution-transformer loading practices which provide valuable data for industry use. If a single practice cannot be established, possibly distribution systems can be divided into two or more classifications and a loading practice provided for each type of system. Such type of information should be of considerable value to those interested in the operation and design of distribution systems.

It is the writer's opinion that individual studies can be justified for the larger sizes of distribution transformers and that the principles outlined in the "Interim Report on Guides for Overloading Transformers and Voltage Regulators"[1] can be utilized. In regard to the pole-type sizes, past experience seems to indicate that, with well-balanced distribution-system design, the thermal loading does not present a serious problem. Burnouts due to overloading have been rare. Where burnouts have been experienced, it has usually been found that they were due to some unexpected load and that they have often occurred in cases where the service to a single customer has been involved. The design of a distribution system requires proper allocation of voltage drop in the overhead primary, transformer, and secondary circuits, and generally it will be found that excessive voltage drop will result before a pole-type transformer is either burned out or before an excessive amount of insulation life has been consumed.

Economic considerations usually require that the transformer voltage drop be large compared to that in the overhead primary and secondary circuits. As a result transformers either are loaded to rating or, in the case of smaller transformers, carry overloads during peak periods. Since transformer loading has a major influence on the customer's voltage, complaints will usually result prior to transformer damage or burnouts.

During the present emergency, most power-supply companies are permitting a greater voltage variation on their distribution system, and a resultant greater loading of distribution transformers is being experienced. A review of past experience seems to indicate that the quality of service to a customer will not be reduced to such an extent that an excessive number of transformer burnouts will be experienced. A moderate increase in life consumption and an occasional transformer failure would be consistent with the wartime practices being used in the operation of other equipment.

REFERENCE

1. INTERIM REPORT ON GUIDES FOR OVERLOADING TRANSFORMERS AND VOLTAGE REGULATORS, AIEE transformer subcommittee. AIEE TRANSACTIONS, volume 60, 1941, September section, pages 692–4.

W. A. Sumner (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): One of the references in the paper is the Putman–Dann paper, "Loading Transformers by Copper Temperature."[1] In this paper was included a report on a program of temperature-cycle tests started on four five-kilovolt-ampere 2,400-volt *CSP* (completely self protected) transformers June 1, 1936. As reported in that paper, a total of 867 cycles had been completed by June 15, 1939. These tests have been continued and now after seven years total 1,911 cycles, without failure. Because of the bearing of these tests on the present interim report, it is believed that an up-to-date progress report will be of interest to this group.

For the last 1,811 cycles, two of these units (*A* and *B*) have been operated at 390 per cent load for an average of 48 minutes for each overload and in that time attained a maximum copper temperature by resistance of 142 degrees centigrade. Two other units (*C* and *D*) operated at 450 per cent load for an average duration of 34 minutes, attaining a maximum copper temperature by resistance of 135 degrees centigrade.

During the course of these runs, the standard dielectric tests for a new 2,400-volt transformer have been applied 17 times, the last being made on the 1,533rd run. Surge tests, with a 56-kv $1^1/_2$-40-microsecond positive wave have been applied 18 times, the last of the surge tests having been made on the 1,810th run. No sign of distress was disclosed by these tests.

The dielectric strength of the oil, which has not been renewed, has been tested frequently and at the 1,810th run averaged 29 kv on the four units.

These units have not been torn down for examination of the insulation, as it is the intention to continue the runs until failure.

However, power-factor tests have been made and indicated 1.5 per cent or lower at 20 degrees centigrade, which is as good as or better than the average new transformer. This appears to be a very good indication that, in spite of this large number of cycles, the insulation is in satisfactory condition. Since these winding temperatures are substantially in excess of those contemplated by the interim report, it is felt that the recommendations of the committee can be considered as conservative.

REFERENCE

1. LOADING TRANSFORMERS BY COPPER TEMPERATURE, H. V. Putman, W. M. Dann. AIEE TRANSACTIONS, volume 58, 1939, October section, pages 504–09

Hamilton Treadway (Rural Electrification Administration, St. Louis, Mo.): The war emergency has focused attention on the problem of overloading transformers as well as other electric equipment. This interest is prompted by the necessity for saving critical materials, and perhaps a great deal could be accomplished in this direction by the adoption of new theories in the design of such equipment. Still much can be accomplished in the direction of conservation of critical materials by utilizing the maximum capacity of existing equipment.

In the distribution-transformer field much can be gained by rearrangement of system layouts and by relocating transformers to attain the maximum use of existing capacity. This requires regular load surveys and a means of readily interpreting the results. The methods developed by Beavers[1] and Clark[2] are easily applied to substations where recording demand meters are used, but no accurate means has been developed for small installations where load curves are not available. It is desirable that a means be developed to permit evaluation of the overload capabilities of these smaller transformers in terms of the type of load served, the indicated maximum demand, and load factor. Probability curves for each type of load may be worked out which will permit the experienced operator to secure a maximum from his present installed transformer capacity. To accomplish this, thousands of load curves must be assembled, grouped, analyzed, and evaluated. This undertaking is outside the scope of any one group. It is suggested that the subcommittee should work out procedures to be followed and assign the task of accumulating and preparing the data on each individual type of load to that group in the industry best situated to do the work.

Whatever data of this nature are supplied by the subcommittee in future reports will prove of inestimable value to the industry.

REFERENCES

1. SELECTING DISTRIBUTION-TRANSFORMER SIZE, M. F. Beavers. ELECTRICAL ENGINEERING, volume 59, October 1940, pages 407–12.

2. SUBSTATION-TRANSFORMER EMERGENCY LOADING PRACTICE, L. W. Clark. AIEE TRANSACTIONS, volume 62, 1943, March section, pages 126–32.

F. E. Andrews (Public Service Company of Northern Illinois, Chicago, Ill.): Much discussion and consideration of the problem of overloading distribution transformers has been directed along the lines of determining the value and kind of overload which a transformer will safely stand, the implication usually being that there should be sufficient margin in any practices adopted to avoid much risk of transformer failure.

We, as engineers, are inclined to take credit for establishing a good operating record as measured by a very low rate of transformer failures due to overloads. I wish to raise the question whether it would not be better engineering, from the standpoint of economics, to carry a considerably higher average overloading on our transformers, thereby requiring less installed transformer capacity at the expense of a higher failure rate.

To state this in another way, it would appear to be a good business judgment, at least for the average run of smaller distribution transformers, to use design loadings representing fairly high overload values. General experience indicates that in a large number of instances design loadings are not attained under actual operating conditions. This, together with other marginal considerations, results in the duty on the average transformer installation frequently being less than the maximum design duty. Furthermore, with the smaller transformers the financial risk and importance of a service interruption are much less than with power-transformer installations or with large distribution-transformer installations. Therefore, it is reasonable that the smaller transformers be applied to the system under a somewhat less conservative loading basis than the others. It would accordingly appear that the engineers and operators of any system, in order to obtain maximum economic benefits from their distribution-transformer installations, should, upon their own responsibility, be willing to go beyond the conservative recommendations for loading established by general industry standards, and by manufacturers for design purposes.

It is also recognized that in considering the economics of transformer overloading, the cost of the higher losses and of higher over-all voltage regulation must be taken into account. In some cases, these factors, rather than temperature and life, should determine the overload limits.

Relay Protection of Tapped Transmission Lines

Discussion and authors' closure of paper 43-118 by M. A. Bostwick and E. L. Harder, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 645–50.

W. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): Some caution must be exercised in thinking of the two-wire a-c pilot-wire scheme proposed by the authors as a true ratio-differential scheme in the ordinary sense of common three-winding-transformer differential relays, where the sum of all three currents is available for restraint. Even the two-wire a-c pilot-wire scheme used on two-terminal lines is only partly "ratio differential" in that restraint is proportional to local current, which represents either through current or the contribution of one terminal to an internal fault. However, in the case of the three-terminal application, consider a heavy through fault *in* on terminal 1 and *out* on terminal 2 with practically no current in terminal 3. In this case terminal 3 has no restraint but is subject to an operating voltage corresponding to a small *per cent* difference of two large values. Thus the careful balancing of pilot-wire voltages, excellent current-transformer characteristics, and the accurate matching of saturating and cutoff devices which are used to limit the pilot-wire voltage are relied upon to prevent false operation, instead of the "ratio-differential" principle.

The application of a-c pilot-wire schemes to three-terminal lines may be limited on account of the relative values of load and short-circuit current. Overcurrent relays are sometimes set to operate with as little as $1^1/_2$ times maximum load current, and it is expected that the minimum short-circuit current shall be at least $1^1/_2$ times the relay setting, so that a ratio of minimum short-circuit current to maximum load current of at least $2^1/_4$ is expected for satisfactory operation; since the three-phase fault current is about 15 per cent greater than the current for a phase-to-phase fault, the required ratio may be about $2^1/_2$ to 1. In contrast, the two-wire a-c pilot-wire scheme, operating on sequence currents or variable current-transformer ratio principles, requires a 2:1 ratio just to take account of different response to three-phase load and single-phase-to-phase faults. Furthermore, if incorrect operations on a two-terminal line are to be avoided during momentary interruptions of the pilot-wire circuit in the presence of load current, an additional factor of 2 is introduced because only one half as much load current is required to cause tripping with the pilot wire open as is necessary under normal fault-clearing conditions with the pilot-wire circuit closed. This gives an over-all "phase factor" of 4 instead of 1.15, or, if the critical ratios of pickup load current to minimum fault current to relay pickup are to be maintained, then the minimum short-circuit current (from the terminal of the weakest source) must be nine times the maximum load current. In the case of "*T* taps," it is expected that tripping energy would be divided among the three terminals (when the lines are energized from one terminal), so that the required ratio of minimum short-circuit to maximum load current becomes about 13, which would exclude many applications.

Carrier-current schemes combined with distance relays are subject to other difficulties than merely a possible reversal of current at one terminal of a three-terminal line, and one specific case was analyzed where such an arrangement would not provide even second-zone sequential tripping after the terminal nearest the fault had opened.

The authors have mentioned parallel operation with industrial generating plants where certain nonessential loads would be "dumped." It is not necessary that an industrial customer should have "nonessential" load which can be "dumped" in order to enjoy the advantages of parallel operation while a part of the load is being carried from a utility system. Frequently a sectionalizing switch is arranged so that when open it leaves a load of approximately that desired for local generation on the industrial generators and the remainder on the utility line. The operation of such a switch on a fault beyond the line terminals does not "dump" any load and therefore may be allowed without serious inconvenience. Thus the sectionalizing breaker may be operated by high-speed fault detectors which are sensitive enough to reach faults *within* the protected section under all conditions. This permits instantaneous reclosing on the utility tie line to provide uninterrupted service to the sectionalized load in case of a flashover on the tap line. Sometimes inertia relays are used for sectionalizing in case of ground faults where the transformer supplying the industrial customer is not grounded.

One method not mentioned by the authors is that of transferred tripping over pilot-wire circuits. The performance of a specific installation made in 1931 was described in 1935[1] and a special application for network service was described in 1938.[2] While carrier-current equipment has been utilized for transferred tripping to protect transformers operating as part of a two-terminal line, it is unfortunately not applicable to the protection of a three-terminal line where the carrier impulse must be transmitted over the line which is faulted.

References

1. Pilot-Wire Relay Protection, E. E. George, W. R. Brownlee. AIEE Transactions, volume 54, 1935, November section, page 1262.

2. Remote Control of Network Protectors, W. R. Brownlee, W. E. Dent, Jr. AIEE Transactions, volume 57, 1938, December section, page 685.

A. J. McConnell (General Electric Company, Schenectady, N. Y.): In the past, there has been little formal discussion in regard to the general problem of relaying three-terminal lines. Bostwick and Harder have analyzed admirably this general problem.

In regard to the protection of three-terminal lines by means of the *HCB* relay, it may be that the problem has been somewhat oversimplified. For example, it is stated that the "*HCB* two-wire a-c pilot-wire scheme circumvents all of these problems . . . ."

From an inspection of Figures 8 and 9, it appears safe to assume that a current of as little as six amperes leaving one terminal (m) will block all three terminals, regardless of the distribution of the currents at the other two terminals (p and n). This means that for an internal fault near terminal n a current of six amperes or greater leaving terminal m will result in a failure to trip. A current of six amperes leaving one terminal for an internal fault is not at all uncommon, especially in those cases in which the impedance from the junction point to n via m is equal to or less than the impedance internally from the junction to n.

It would also appear that there is a possibility for a relay at a terminal which has no infeed to trip on a heavy fault external to one of the other terminals. For example, if there should be a difference between the output currents of the two relays carrying the through fault current, because of either the main current transformers or the saturating transformers in the *HCB* relays, a current would appear in the operating coil of the relay at the third terminal. Since there would be no corresponding restraining current, the relay would trip incorrectly if this operating coil current were above the relay pickup for that condition.

G. B. Dodds (Duquesne Light Company, Pittsburgh, Pa.): The authors have brought out the difficulties of relaying tapped transmission circuits, have pointed out the possible use of a-c pilot-wire protection in solving some of the problems, and have encouraged presentation of specific examples of solutions to particular problems.

The example shown in Figure 1 of this discussion is typical of the manner of serving a number of war load customers. The original condition was two 68-kv transmission circuits between stations *A* and *B* in the middle of an important transmission system. A new war load customer is being tapped to one of these circuits as shown. The circuits between stations *A* and *B* had existing *HCB* pilot-wire protection; the connection of the new customer as a tap nullified this protection on the one circuit. The resistance of the pilot conductors was already up to the limit of standard two-terminal protection, being approximately 2,000 loop ohms resistance and 1.4 microfarads capacitances.

It appeared hopeless to apply standard three-terminal pilot-wire protection because of the high resistance necessary for each of the legs of the pilot conductors. The scheme shown on Figure 1 was finally worked out and tests made to prove its satisfactory operation. This scheme consists of installing a standard *HCB* installation at the new tapped customer and having it normally disconnected from the pilot-wire circuits. It is connected to the pilot-wire circuits only in event of a fault on the low-voltage bus at the new station. The impedance of the transformer bank limits the fault current to such a magnitude that satisfactory operation is obtained on the three-terminal pilot wire for this fault location.

For other fault locations, such as through faults in either direction from stations *A* or *B* or inside faults on the protected circuit where the fault currents are much higher, the installation operates as a standard two-terminal arrangement.

The third terminal at the new station *C* is connected and disconnected from the pilot wire by means of an instantaneous overcurrent relay which picks up only for faults at station *C*. In order to co-ordinate the operation of the pilot-wire relays at stations *A* and *B* with the cutting in of the relays at station *C*, a slight amount of time delay was added to the pilot-wire relays at stations *A* and *B*. A time in the neighborhood of four cycles was found satisfactory.

The single-phase grounding switch shown on the sketch is closed by operation of the transformer-bank differential relays. Its purpose is to provide sufficient current to operate the relays at adjacent stations. The 68-kv system is grounded through two 63-ohm resistors in parallel, and as a result there is little voltage disturbance associated with a line-to-ground fault.

M. A. Bostwick and E. L. Harder: Mr. Brownlee made the statement that ". . . even the two-wire a-c pilot-wire scheme used on two-terminal lines is only partly ratio differential in that restraint is proportional to local current which represents either through current or the contribution of one terminal to an internal fault." This is not true of the type-*HCB* two-wire a-c scheme referred to throughout the paper, although it may apply to another scheme. For the *HCB* relay, the operating equation is derived in this discussion. Equation 6 shows that there is a restraining force pro-

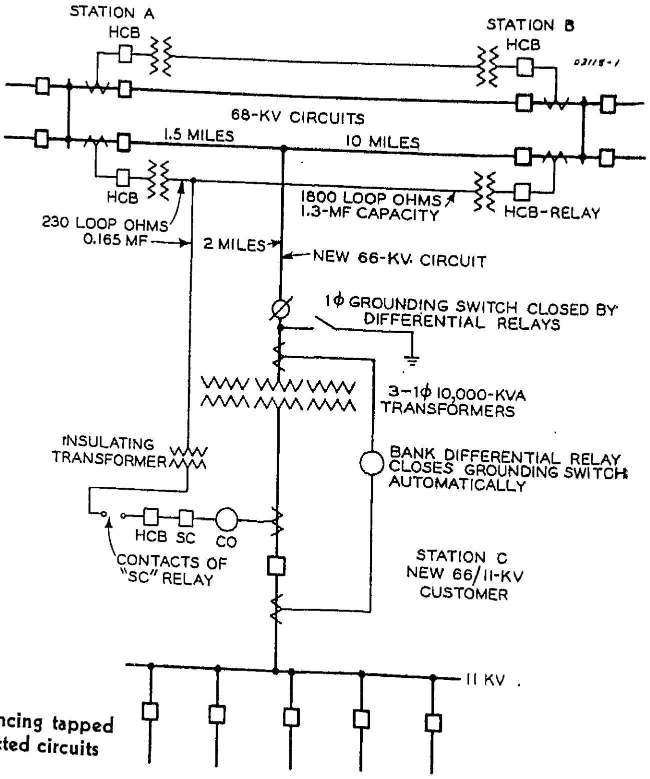


Figure 1. Method of balancing tapped load on pilot-wire protected circuits

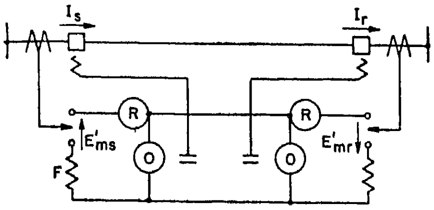


Figure 2. Ratio differential—circulating current

portional to the sum of the currents at the two ends of the line

As the limiting devices of the relay take effect at higher currents, the effect is the same as though the currents at the two ends remained constant at the values that operate the limiting device. Thus the operation then depends only on the phase position between these two currents and the relay takes on a "directional-comparison" characteristic. This results in large restraint for through faults and tripping for internal faults.

On the three-terminal line Mr. Brownlee's point is correct that a terminal with no current has no restraint. However, it should not be concluded that "careful balancing of pilot-wire voltages, excellent current-transformer characteristics, and accurate matching of saturating cutoff devices" and so forth are depended upon for prevention of false operation. At high currents which operate the saturating devices, two voltages of about 60 volts act in series to circulate current over the pilot wires with a resulting zero voltage at the center point of the pilot wires. One of the limiting devices would have to be 50 per cent off, giving only 30 volts to produce 15 volts across the pilot wires at the mid-point which would be necessary to operate falsely the third terminal. They are, of course, easily held to much smaller tolerances than this. Also the limiting devices in the relay make the scheme relatively insensitive to wide differences in current-transformer characteristics. The scheme was designed originally to operate with one current transformer that saturated at 30 volts and another that was unsaturated at 120 volts. This represents a 4-to-1 ratio in currents required to saturate these two transformers. Thus, while ratio characteristics do not help the third terminal for the case cited, still ordinary care in design and application provide ample margins of safe operation because of the fundamental design of the scheme. The limiting devices play a big part in this, because they prevent ever getting a difference of two *large* quantities.

Mr. Brownlee's discussion of operating limits reflects a condition which is sometimes encountered although not so severely as indicated, for several reasons. In the first place it is not absolutely necessary to set above load currents. The largest single user of pilot-wire currents[1] purposely sets below load current instead of using supervision equipment to detect outages of the pilot wire. With good pilot wires the infrequent tripping from this source under normal load conditions is not objectionable on the types of systems where pilot-wire relays are frequently used, that is, where load busses are dual fed. Secondly, if there is backup protection, it is not necessary that the pilot-wire relay trip for the smallest possible current from the weakest terminal with all other terminals open. Also it should be borne in mind that the ground-current setting can be made sensitive independent of load currents.

In the carrier-current application mentioned, where second-zone sequential tripping was not obtained, after the first terminal was opened, the mutual effect was evidently not considered in the setting. The proper method is given in the paper.

The industrial tie scheme described is commnedable. Weak ties particularly should be arranged to open promptly during disturbances to avoid the more serious consequences of out-of-step operation.

The arrangement described by G. B. Dodds for applying the *HCB* relay to three-terminal lines of high pilot-wire resistance represents a valuable contribution to the science of applying this relay. It is also a partial answer to the load-fault ratio problem mentioned by Mr. Brownlee, since it reduces the operation to two-terminal-line operation, except for external faults on the third terminal.

Shunt reactors to cancel pilot-wire capacitance and higher-ratio insulating transformers have also been useful in applications involving pilot-wire capacitance and resistance, respectively, beyond the normal range for application.

Operating Characteristics of Pilot-Wire Relay

Ratio Differential—Circulating Current—Figure 2. The voltages E_{ms}' and E_{mr}', produced in the relay network by currents I_s and I_r, respectively, act normally in series to circulate a restraint current proportional to the load over the line. The voltage between pilot wires midway between stations is zero, and, if the pilot-wire impedance r is made relatively small compared with R plus F, the voltage across the operating coils is negligible. Through current, therefore, produces current in restraining coils only.

If equal currents flow in from the two ends to a fault, the voltages E_{ms}' and E_{mr}' will be equal and opposed, so that no current will circulate over the pilot wires. While equal currents into the line produce the same current in both R and O coils, there is a net operating effect due to the very much larger number of turns on the operating coil.

The currents I_s and I_r may in general be considered the superposition of two sets of currents:

I_{sm} and I_{rm} equal and in phase.

I_{sn} and I_{rn} equal and 180 degrees out of phase.

These are the two types of current for which relay action has been stated. Under normal load conditions the first type only is present. The superposition stated mathematically is:

$$I_s = I_{sm} + I_{sn} \tag{1}$$

$$I_r = I_{rm} + I_{rn} = I_{sm} - I_{sn} \tag{2}$$

The amount of each type of current present for any particular pair of currents I_s and I_r is determined by solving these equations, giving

$$I_{sm} = I_{rm} = (I_s + I_r)/2 \tag{3}$$

$$I_{sn} = -I_{rn} = (I_s - I_r)/2 = I_f/2 \tag{4}$$

where I_f is the fault current.

The restraint coil current is proportional* to I_{sm} and the operating coil current to I_{sn}. In a polar relay forces are proportional to the first power of coil currents. Therefore, the equation expressing the balance of forces at the minimum operating point is as follows, absolute values being effective due to rectification of the quantities for use in operating coils:

$$|I_{sn}| = C|I_{sm}| + B/2 \tag{5}$$

Operating force = restraining force + spring and magnetic bias force

or

$$|I_s - I_r| = C|I_s + I_r| + B \tag{6}$$

or

$$|I_f| = C|I_s + I_r| + B \tag{7}$$

The vertical bars indicate that the absolute value is referred to.

The relay, therefore, operates whenever the fault-current magnitude exceeds a fixed proportion of the average through fault current, plus a constant. This characteristic is a circle of diameter equal to $C|I_s + I_r| + B$. I_s and I_r vectors terminate on the circle at opposite ends of any diameter. Tests and

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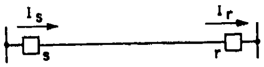
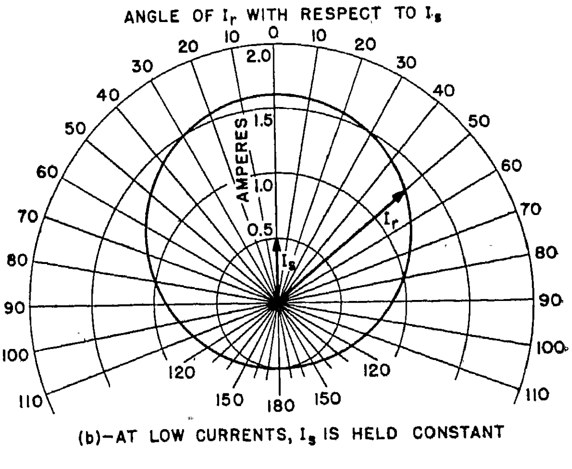
* The slight approximation of neglecting the I_{sn} component in the restraint coils is permissible with the relay constants used.



(a)—CURRENT REFERENCE DIRECTIONS



(b)—AT LOW CURRENTS, I_s IS HELD CONSTANT

Figure 3

theory also indicate the tripping locus of I_r to lie on a circle as shown in Figure 3 for a fixed I_s value and relay setting.

This characteristic is the same as is used in the ratio differential protection of generators and transformers. As both through current and fault current become very large, the two terms of equation 7 become large compared with Figure 3. The tripping point then approaches the pure ratio:

$$I_f=C|I_s+I_r| \tag{8}$$

Limiting Devices. If the filter outputs are passed through limiting devices such as saturating transformers before application to the pilot-wire and relay-circuit combination, the characteristics may be understood by consideration of the limit. For values of I_r and I_s above the limit the relay action is the same as though each had the limiting magnitude but retained its phase position. If $\bar{I}_s=\bar{I}_r=$a constant limiting value,** then if θ is the angle between I_s and I_r we may write:

$$I_s=\bar{I}_s;\quad I_r=\bar{I}_s\epsilon^{j\theta}$$

and equation 6 becomes

$$\bar{I}_s|1-\epsilon^{j\theta}|=C\bar{I}_s|1+\epsilon^{j\theta}|+B$$

and, since $\bar{I}_s$ is a constant (the limiting value)

$$|1-\epsilon^{j\theta}|=C|1+\epsilon^{j\theta}|+D \tag{9}$$

The balance thus depends solely on θ, D being a constant, and the relay has pure directional characteristics.

If C is 1 and $D=0$, the equation is satisfied for $\theta=\pm 90$ degrees, and the relay would trip, with large line currents, only when I_r is over 90 degrees out of phase with I_s, either leading or lagging.

For other values of C and D other angular limits between I_s and I_r are established as the threshold of tripping.

Mr. McConnell points out the blocking due to six amperes flowing out one terminal for an internal fault in Figures 8 and 9 of the paper. These curves and Figure 7 are all based on a 0.5-ampere ground-current setting as mentioned in the text. Thus six amperes represent 12 times the relay setting. The curves also apply to phase currents, but the scales must then be increased in the ratio of phase-current setting to ground-current setting. Experience to date has not shown cases beyond the limits of correct tripping mapped out by the curves of Figures 8 and 9.

The question in regard to possible false tripping due to large through current with no current at the third terminal was discussed previously. The main point was that with limiting devices the "difference of two large quantities" is never obtained, because there are no "large quantities." Figure 9 shows that with ten amperes in n and out m there is a very wide margin to prevent tripping even if two amperes entered at p (this could be possible with secondary currents). The margin would be even greater if no current entered at p. This is shown by a comparison of Figures 8 and 9 with the same currents at n and m.

REFERENCE

1. PILOT-WIRE RELAYING ON A METROPOLITAN SYSTEM, T. G. LeClair, E. L. Michelson. AIEE TRANSACTIONS, volume 62, 1943, August section, pages 511–15.

** Bars over characters represent scalar quantities.

A New High-Speed Balanced-Current Relay

Discussion and author's closure of paper 43-117 by V. N. Stewart, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 553–5.

A. J. McConnell (General Electric Company, Schenectady, N. Y.): As a former designer of induction-cylinder relays, it occurs to this discusser that the author might have pointed out certain advantages that accrue simply because of the use of that type of relay element. For example, high-speed relays of the magnetic-attraction type, as well as other designs, have a double-frequency force or torque which naturally results in a certain amount of vibration that may affect adversely the relay characteristics.

Such an adverse effect is particularly evident in relays in which one force or torque is balanced against another. For instance, if the peaks of the double-frequency-torque waves do not occur at the same time, it logically follows that operation will occur at a lower rms operating current than if the peaks occurred at the same time. Translated into relay nomenclature, this means that the relay will have a phase-angle error, in many cases sufficient to nullify the safety margin (slope) normally associated with balanced-current relays. Of course, the balanced-current relay is subjected to currents of different phase angles whenever the parallel lines do not have the same R/X ratio.

Because of the steady torque of the induction-cylinder-relay element, the induction-cylinder balanced-current relay should have a very small phase-angle error. Perhaps the author can provide this information.

Experimental proof that the induction-cylinder relay has substantially no net double-frequency torque has been presented in the form of oscillograms.[1] Presentation in that manner was believed to be more satisfactory than the already available analytical proof. However, a statement made before the Institute,[1] that inertia is the reason for the relay's practical freedom from vibration, indicates that a reference to the analytical proof is necessary.

The theory governing the operation of the induction-cylinder relay is the same as that of the induction motor, and previous work on induction motors may be used, if it is kept in mind that the standstill equations of the induction motor apply to the relay. Morrill's equation 109[2] shows that, at standstill, all double-frequency-torque components disappear, leaving only a constant-torque term. This is also illustrated by Figure 12 of the same paper.

REFERENCES

1. Discussion by A. J. McConnell and author's closure by Bert V. Hoard of AN IMPROVED POLYPHASE DIRECTIONAL RELAY. AIEE TRANSACTIONS, volume 60, 1941, page 682.

2. THE REVOLVING-FIELD THEORY OF THE CAPACITOR MOTOR, Wayne J. Morrill. AIEE TRANSACTIONS, volume 48, 1929, pages 614–29.

V. N. Stewart: Mr. McConnell suggests that the induction-cylinder balanced-current relay has an inherent characteristic, which gives it an advantage over previous designs. In the protection of parallel lines with different R/X ratios, the improved phase-angle characteristic obtained with this relay does not affect appreciably the slope. For an extreme case, where the two line currents might differ as much as 45 degrees, the 125 per cent slope of this relay is changed four or five per cent.

Although the reference to Mr. Morrill's paper by Mr. McConnell serves as sufficient analytical proof to the relay's freedom from vibration, additional proof has been developed by C. D. Hayward in which he compares the performance of both the induction-cylinder and induction-ring devices. The following paragraphs present his calculations with several conclusions.

INDUCTION-CYLINDER DYNAMOMETER TORQUE

Assume a four-pole induction-cylinder-type dynamometer with coils having N_1 and N_2 turns, respectively, on the poles of the two axes.

For the applied coil currents let

$$i_1=I_1\sin(\omega t+\theta_1)-I_1\sin\theta_1\epsilon^{-\frac{t}{T_1}} \tag{1}$$

$$I_2=I_2\sin(\omega t+\theta_2)-I_2\sin\theta_2\epsilon^{-\frac{t}{T_2}} \tag{2}$$

For the currents induced in the cylinder let

${}_ci_1=$current induced by i_1
${}_ci_2=$current induced by i_2

Also let

$M_1=$mutual-inductance coil 1 to cylinder
$M_2=$mutual-inductance coil 2 to cylinder

Coil 1 is considered as having N_1 turns divided equally between the left and right poles in Figure 1 of this discussion, whereas coil 2 has N_2 turns divided equally between the front and back poles.

L_1 and $L_2=$self-inductance of the paths followed by the induced-cylinder currents ${}_ci_1$ and ${}_ci_2$.

$$L_1=\frac{M_1}{N_1}+{}_LL_1 \tag{3}$$

$$L_2=\frac{M_2}{N_2}+{}_LL_2 \tag{4}$$

${}_LL_1$ and ${}_LL_2=$leakage inductance of paths followed by ${}_ci_1$ and ${}_ci_2$
R_1 and $R_2=$resistance of paths followed by ${}_ci_1$ and ${}_ci_2$

Operational-voltage equations for circuits of induced currents

$$M_1pi_1-L_1p{}_ci_1-R_{1c}i_1=0$$
$$M_2pi_2-L_2p{}_ci_2-R_{2c}i_2=0$$

from which

$${}_ci_1=\frac{M_1p}{L_1p+R_1}\,i_1 \tag{5}$$

$${}_ci_2=\frac{M_2p}{L_2p+R_2}\,i_2 \tag{6}$$

These are both of the same form. The corresponding indicial admittance for either may be obtained from[1]

$$A(t)=\frac{Mp}{Lp+R}\,1=\frac{M}{L}\frac{p}{p+\frac{R}{L}}\,1=\frac{M}{L}\,\epsilon^{-\frac{R}{L}t} \quad (7)$$

The applied currents, equations 1 and 2, are both of the general form

$$i(t)=\underbrace{I\sin(\omega t+\theta)}_{\substack{\text{steady-state}\\ \text{a-c term}}}-\underbrace{I\sin\theta\epsilon^{-\frac{t}{T}}}_{\substack{\text{transient}\\ \text{d-c term}}} \quad (8)$$

The steady-state term will induce both steady-state and transient currents in the cylinder. These are calculated conveniently by use of a special form of the superposition theorem[2] for an applied force of the form $I\sin(\omega t+\theta)$.

$$i_s(t)=I\omega\cos(\omega t+\theta)\int_0^\infty\cos\omega\lambda A(\lambda)d\lambda+ I\omega\sin(\omega t+\theta)\int_0^\infty\sin\omega\lambda A(\lambda)d\lambda \quad (9)$$

$$i_t(t)=I\sin\theta A(t)-I\omega\times\cos(\omega t+\theta)\int_t^\infty\cos\omega\lambda A(\lambda)d\lambda-I\omega\times\sin(\omega t+\theta)\int_t^\infty\sin\omega\lambda A(\lambda)d\lambda \quad (10)$$

If the value of $A(t)$ is substituted for the circuit from equation 7 and solved, we obtain

$${}_ci_s(t)=I\frac{M}{L}\sin\alpha\cos(\omega t+\theta-\alpha) \quad (11)$$

$${}_ci_t(t)=I\frac{M}{L}\sin\theta\epsilon^{-\frac{R}{L}t}-I\frac{M}{L}\times\sin\alpha\cos(\theta-\alpha)\epsilon^{-\frac{R}{L}t} \quad (12)$$

where

$$\left.\begin{aligned}\alpha&=\tan^{-1}\frac{\omega L}{R}\\ Z&=\sqrt{R^2+\omega^2L^2}\end{aligned}\right\}$$

The phase angle and impedance of the path of the induced current in the cylinder with the coils on the poles open-circuited.

The transient term of the applied current of equation 8 will induce several kinds of transient currents in the cylinder. These are calculated readily from the general form of the superposition theorem[3]

$${}_ci(t)=i(0)A(t)+\int_0^t A(t-\lambda)i'(\lambda)d\lambda \quad (13)$$

If the transient term, $I\sin\theta\epsilon^{\frac{-t}{T}}$ is substituted from equation 8 for $i(t)$ and the indicial admittance,

$$A(t)=\frac{M}{L}\,\epsilon^{-\frac{R}{L}t}$$

from equation 7, we obtain

$${}_ci_D(t)=I\sin\theta\frac{M}{L}\,\epsilon^{-\frac{R}{L}t}+\int_0^t\frac{M}{L}\,\epsilon^{-\frac{R}{L}(t-\lambda)}I\sin\theta\left(-\frac{1}{T}\right)\epsilon^{-\frac{\lambda}{T}}d\lambda$$

and, if the integration is carried out

$${}_ci_D(t)=I\frac{M}{L}\sin\theta\epsilon^{-\frac{R}{L}t}+I\frac{M}{L}\times\sin\theta\frac{\left(\epsilon^{-\frac{t}{T}}-\epsilon^{-\frac{R}{L}t}\right)}{1-\frac{R}{L}T} \quad (14)$$

If equations 11 and 12 are added, and equation 14 is subtracted, we obtain an expression for the total current induced in the cylinder caused by the applied coil current, equation 8

$${}_ci(t)=I\frac{M}{L}\sin\alpha\cos(\omega t+\theta-\alpha)-I\frac{M}{L}\times\left[\sin\alpha\cos(\theta-\alpha)-\frac{\sin\theta}{\left(1-\frac{R}{L}T\right)}\right]\epsilon^{-\frac{R}{L}t}-I\frac{M}{L}\frac{\sin\theta}{\left(1-\frac{R}{L}T\right)}\epsilon^{-\frac{t}{T}} \quad (15)$$

Corresponding with the two applied coil currents, equations 1 and 2, the two induced currents will be

$${}_ci_1=I_1\frac{M_1}{L_1}\sin\alpha_1\cos(\omega t+\theta_1-\alpha_1)-I_1\frac{M_1}{L_1}\times\left[\sin\alpha_1\cos(\theta_1-\alpha_1)-\frac{\sin\theta_1}{\left(1-\frac{R_1}{L_1}T_1\right)}\right]\epsilon^{-\frac{R_1}{L_1}t}-I_1\frac{M_1}{L_1}\frac{\sin\theta_1}{\left(1-\frac{R_1}{L_1}T_1\right)}\epsilon^{-\frac{t}{T_1}} \quad (16)$$

$${}_ci_2=I_2\frac{M_2}{L_2}\sin\alpha_2\cos(\omega t+\theta_2-\alpha_2)-I_2\frac{M_2}{L_2}\times\left[\sin\alpha_2\cos(\theta_2-\alpha_2)-\frac{\sin\theta_2}{\left(1-\frac{R_2}{L_2}T_2\right)}\right]\epsilon^{-\frac{R_2}{L_2}t}-I_2\frac{M_2}{L_2}\frac{\sin\theta_2}{\left(1-\frac{R_2}{L_2}T_2\right)}\epsilon^{-\frac{t}{T_2}} \quad (17)$$

The mutual flux,* common to the exciting coil and the path of the induced current in the cylinder, may be calculated by use of the relationship

$$\phi=\frac{4\pi}{10}\text{(net ampere turns) (permeance of the mutual magnetic circuit)}$$

but $\frac{4\pi}{10}\text{(permeance)}=\frac{M}{N}10^8$

Hence, since the net ampere turns is the ampere turns of the exciting coil less the ampere turns of the induced current in the cylinder

$$\phi_1=(i_1N_1-{}_ci_1)\frac{M_1}{N_1}10^8 \quad (18)$$

$$\phi_2=(i_2N_2-{}_ci_2)\frac{M_2}{N_2}10^8 \quad (19)$$

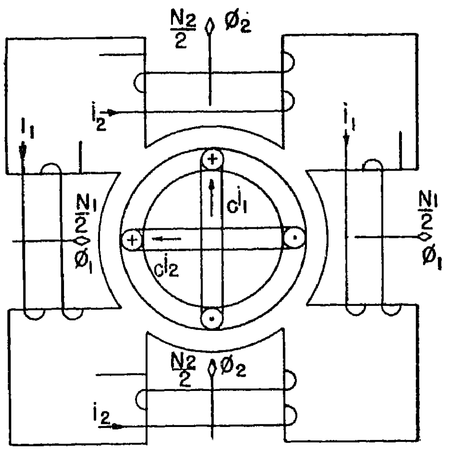


Figure 1

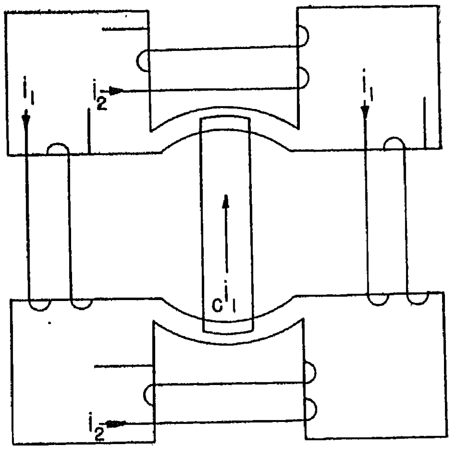


Figure 2

There will be two torques on the relay, which, when clockwise rotation in Figure 1 is considered as positive, are as follows

$$T_{12}=+{}_ci_1\cdot\phi_2\cdot\frac{2l_1r_1}{a_2}\text{ dyne centimeter} \quad (20)$$

$$T_{21}=-{}_ci_2\cdot\phi_1\cdot\frac{2l_2r_2}{a_1}\text{ dyne centimeter} \quad (21)$$

where

l = length of induced-current path in field of mutual flux—centimeters

r = radius of cylinder—centimeters

a = area of pole face through which mutual flux passes—square centimeters

The subscripts 1 and 2 refer to values associated with the two pole axes.

The total torque is the sum of these two torques. Hence, substituting equations 18 and 19 in equations 20 and 21, and adding

$$T={}_ci_1(i_2N_2-{}_ci_2)\frac{M_2}{N_2}\cdot\frac{2l_2r_2}{a_2}10^8-{}_ci_2(i_1N_1-{}_ci_1)\frac{M_1}{N_1}\cdot\frac{2l_1r_1}{a_1}10^8 \quad (22)$$

If we assume the dynamometer is mechanically symmetrical

$l_1=l_2 \quad R_1=R_2 \quad \frac{M_1}{N_1}=\frac{M_2}{N_2}=\frac{4\pi}{10}\mathcal{P}$

$r_1=r_2 \quad L_1=L_2 \quad \mathcal{P}$ = permeance of mutual flux circuit

$a_1=a_2 \quad \alpha_1=\alpha_2$

$$T=({}_ci_1\cdot i_2N_2-{}_ci_2\cdot i_1N_1)\frac{4\pi}{10}\mathcal{P}\frac{2lr}{a}10^8 \quad (23)$$

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* There will also be leakage fluxes, but these need not be considered, since it can be shown that they do not produce any net torque on the cylinder.

To solve for the steady-state torque substitute the steady-state components of equations 1, 2, 16, and 17 in equation 23

$$T_s=\left[I_1N_1\frac{4\pi}{10}\mathcal{P}\frac{1}{L}I_2N_2\sin\alpha\cos(\omega t+\theta_1-\alpha)\times\right.$$
$$\sin(\omega t+\theta_2)-I_2N_2\frac{4\pi}{10}\mathcal{P}\frac{1}{L}I_1N_1\times$$
$$\left.\sin\alpha\cos(\omega t+\theta_2-\alpha)\sin(\omega t+\theta_1)\right]\cdot\frac{4\pi}{10}\mathcal{P}\frac{2lr}{a}10^8$$

$$T_s=I_1I_2M_1M_2\frac{1}{L}\frac{2lr}{a}10^8\sin\alpha\cdot[\cos(\omega t+\theta_1-\alpha)\times$$
$$\sin(\omega t+\theta_2)-\cos(\omega t+\theta_2-\alpha)\sin(\omega t+\theta_1)]$$
$$=I_1I_2M_1M_2\frac{1}{L}\frac{2lr}{a}10^8\sin\alpha\times$$
$$\frac{1}{2}\left[\sin(2\omega t+\theta_1+\theta_2-\alpha)+\sin(\theta_2-\theta_1+\alpha)-\right.$$
$$\left.\sin(2\omega t+\theta_1+\theta_2-\alpha)-\sin(\theta_1-\theta_2+\alpha)\right]$$
$$=I_1I_2M_1M_2\frac{1}{L}\frac{2lr}{a}10^8\sin\alpha\cdot\sin(\theta_2-\theta_1)\cos\alpha$$

$$T_s=I_1I_2M_1M_2\frac{1}{L}\frac{lr}{a}10^8\cdot\sin2\alpha\sin(\theta_2-\theta_1)\quad(24)$$

or, since

$$\frac{\sin\alpha}{L}=\frac{\omega}{Z}$$

$$T_s=I_1I_2M_1M_2\frac{\omega}{Z}\frac{2lr}{a}10^8\cdot\cos\alpha\sin(\theta_2-\theta_1)\quad(25)$$

A number of interesting conclusions can be drawn from the formulas 24 and 25:

(*a*). The torque is steady, having no alternating component, for any value of phase angle between the two applied currents or for any relative magnitude of the applied currents.

(*b*). Maximum torque occurs at 90 degrees phase angle between the two applied currents, regardless of the phase angle of the induction cylinder.

(*c*). From equation 25 it is seen that maximum torque occurs for $\alpha=0$. This would require a capacitor in the induction-cylinder circuit to tune the exciting and leakage reactances and, hence, reduce L to zero. From equation 24 it is seen that if the circuit is not tuned, that is, L is not zero, then maximum torque occurs for $\alpha=45$ degrees; L in the denominator should be minimum, hence, leakage reactance should be zero. The cylinder impedance should equal the exciting impedance for maximum power transfer; hence, the cylinder should be pure resistance equal to the exciting reactance $R=\frac{M_1}{N_1}=\frac{M_2}{N_2}$.

INDUCTION-RING DYNAMOMETER

The induction-ring dynamometer in Figure 2 of this discussion is not a symmetrical structure; so, the foregoing results do not apply. An expression for the torque can be obtained from equation 22 by setting ${}_ci_2=0$.

$$T={}_ci_1\cdot i_2\cdot M_2\cdot\frac{2l_2r_2}{a_2}\cdot10^8\qquad(26)$$

which has a steady-state value obtained by substituting the steady-state components of ${}_ci_1$ and i_2 from equations 16 and 2

$$T_s=I_1I_2M_1M_2\cdot\frac{2l_2r_2}{a_2}10^8\frac{\sin\alpha_1}{L_1}\times$$
$$\cos(\omega t+\theta_1-\alpha_1)\sin(\omega t+\theta_2)$$

$$T_s=I_1I_2M_1M_2\frac{l_2r_2}{a_2}\frac{\omega}{Z_1}10^8\times$$
$$[\underbrace{\sin(2\omega t+\theta_1+\theta_2-\alpha_1)}_{\text{alternating}}+\underbrace{\sin(\theta_2-\theta_1+\alpha_1)}_{\text{steady}}]\qquad(27)$$

From this we can draw the following conclusions:

(*a*). The torque is pulsating, having a double-frequency alternating component which has the same amplitude for any phase angle between the two applied currents. It also has a steady component, which, at the angle of maximum torque, is equal to the crest value of the alternating component. At all other angles the steady component is smaller, so that the torque reverses during part of the cycle.

(*b*). Maximum torque occurs when $\theta_2-\theta_1+\alpha_1=90$ degrees; that is, it occurs when the phase angle between the two applied currents is the complement of the phase angle of the ring.

REFERENCES

1. OPERATIONAL CIRCUIT ANALYSIS, **Bush.** Appendix C, formula 3.
2. Reference 1, page 71, formulas 107 and 108.
3. Reference 1, page 56, formula 77.

Pilot-Wire Relaying on a Metropolitan System

Discussion and authors' closure of paper 43-79 by T. G. LeClair and E. L. Michelson, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 511–15.

W. K. Sonnemann (Westinghouse Electric and Manufacturing Company, Newark, N. J.): I am very much interested in this straightforward exposition of a pilot-wire system for one reason in particular. This is because I believe the authors have shown again that a well-designed chain has all the links of equal strength. Here they have chosen a pilot-wire channel with electrical and mechanical characteristics of a degree of reliability matched to the reliability of the relays with which it is used. The over-all result is an operating record which is highly satisfactory.

The authors have stated that correct operation of the systems they described depends on a definite division of the secondary currents, which in turn depends upon the relative impedance of the terminal equipment. It is presumed that they had in mind particularly the relays, pilot wires, and resistors. It should be noted, however, that since the relays are not of the percentage-differential type, the relative qualities of the current transformers at both ends of the line enter into the picture. No doubt the current transformers used have adequate capacity, since no trouble from this source has been mentioned. However, for the general application, schemes of this type require that closer attention be paid to the current-transformer characteristics than with fault-current restrained relays, particularly at the higher through-fault currents, and with the higher-resistance pilot-wire circuits.

One of the remote-trip schemes described indicates that contacts are used in the pilot-wire circuit. It seems to me that these contacts should have considerable pressure or wipe or both because of their use in an important, relatively low-voltage low-current a-c circuit.

The statement is made that the pilot wires are checked semiannually as part of the routine relay test. I believe it would be in order if the authors would give us a few words concerning the frequency and nature of the troubles on the pilot wire that are uncovered by this test.

S. C. Leyland (Westinghouse Electric and Manufacturing Company, Newark, N. J.): This paper, by presenting details of a large amount of field experience, has contributed some very worthwhile additions to the information already available on pilot-wire relaying as presented in the relay subcommittee's report "Pilot-Wire Circuits for Protective Relaying—Experience and Practice" at the 1943 winter technical meeting.[1]

The paper states, "The principal advantage of using four pilot wires and three relays instead of two pilot wires and one relay is that the relay target will identify which cable has failed in a transmission line using single-conductor cables." I would like to suggest that to obtain the feature of indicating the phase which is in trouble, a phase selector relay be used. Such a device was presented before the Institute in the Goldsborough-Hill paper, "Relays and Breakers for High-Speed Single-Pole Tripping and Reclosing."[2] By use of such a device, indication of the faulted phase can be obtained readily and the pilot-wire scheme simplified to the extent of the elimination of two pilot wires and two relays at each terminal and the addition of a phase-selector relay at each terminal. It is possible to eliminate one of the phase-selector relays, providing it is permissible to forego the requirement of indicating the faulted phase when the circuit is energized from one end only.

Mention is made in the paper of a method of protecting a transformer bank and a line when no high-voltage breaker is available. This method is to protect the line as a unit and the transformer as a unit and to send a signal over the pilot wire to remote trip for trouble in the transformer bank. There have been installations made where the bank and the line were covered by a single differential pilot-wire relaying scheme. In most cases, it was necessary to provide tripping suppressors to prevent operation of the scheme because of magnetizing inrush currents. In some cases this may not be necessary because of the location and size of the bank limiting the inrush current to a value lower than minimum pickup of the relays.

In reporting the cases of trouble, it is significant that only one case was a result of failure of the terminal equipment, and this was due to a damaged resistor resulting from overload applied during test. The other cases of trouble were with the pilot-wire circuits and are what might be termed "external troubles." In no case did inherent weakness of the pilot-wire scheme contribute to the failure to operate correctly. Since so-called "external troubles" were the cause of the majority of failures in the pilot-wire circuit, it would be interesting to know what percentage of external troubles are included in the 7.2 per cent incorrect operations for relay systems used on the higher voltages.

The statement is made in the paper that the pilot-wire schemes should provide a more reliable form of protection than carrier-current relaying. While this statement, no doubt, is true for the system described, there is some question whether such is the case where all types of pilot-wire channels are considered. A privately owned underground pilot cable, such as is described in this paper, is less subject to trouble than are overhead pilot wires either open wire or cable. The paper also points out that leased telephone circuits have been subject to more outages and for longer times than privately owned pilot circuits. In making a general comparison, therefore, between carrier relaying and pilot relaying, it would be desirable to include all types of pilot-wire circuits. It would also be desirable to make this comparison on a circuit-mile basis since carrier-relaying installations generally are applied to much longer lines than is pilot-wire relaying.

REFERENCES

1. PILOT-WIRE CIRCUITS FOR PROTECTIVE RELAYING—EXPERIENCE AND PRACTICE, AIEE committee on protective devices. AIEE TRANSACTIONS, volume 62, 1943, May section, pages 210–14.

2. RELAYS AND BREAKERS FOR HIGH-SPEED SINGLE-POLE TRIPPING AND RECLOSING, S. L. Goldsborough, A. W. Hill. AIEE TRANSACTIONS, volume 61, 1942, February section, pages 77–81.

Philip Sporn (American Gas and Electric Service Corporation, New York, N. Y.): In their comparison of pilot-wire systems with carrier-current relay systems, the authors state that the pilot-wire systems described in their paper are competitive in cost to approximately 20 miles in length. I do not know how they arrived at their conclusions. For example, they state that the pilot cables used cost up to approximately $1,500 per mile installed exclusive of terminal-relay equipment. On our system where we have approximately 200 carrier-current relay terminals we find that for 132-kv lines the cost per terminal for carrier-current relaying plus backup protection amounts to slightly under $5,000 installed. On this basis the pilot-wire systems are competitive with carrier-current relay systems only on lines less than seven miles in length and on lower voltage systems to line less than six miles in length.

The authors are of the opinion that pilot-wiring relaying systems provide a more reliable form of relay protection than carrier-current relaying. In our repeated and cumulative experience this definitely has not been the case. Pilot-wire relaying is no more reliable than the pilot wire itself, and during severe lightning storms the failure of the pilot wire on overhead lines is very likely to occur just at the time when it is needed most for reliable operation. Not only that, we have found that under severe storm conditions the transmission-line structure will stand up physically when the pilot-wire supports will fail to do so. Of course this would not be the case with underground pilot wires. But the latter is so atypical that one can safely say that, from a reliability standpoint, carrier-current relaying is by far the superior method.

E. L. Harder (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The paper by Mr. LeClair and Mr. Michelson is a particularly valuable contribution to the industry in that it represents experience on a large number of circuits extending over many years. There are a number of interesting points which bear emphasis.

It is noted that the cost of pilot cable in duct as given in the paper is less than the costs usually obtained for aerial cable. This seems paradoxical, as overhead wiring is usually cheaper, but is apparently due to the fact that the duct space is free and the expense of pulling the pilot cable into it is less than the cost of messenger and applying supporting clips on aerial cable.

The idea of setting the protective relays below maximum load current to obtain supervision by tripping in event of pilot-wire outage is a worth-while simplification in those cases where parallel cables make such operation permissible.

In the scheme described in the paper, remote tripping for transformer faults is secured by opening the pilot circuit. This results in tripping when the current entering the far end of the line equals the pilot-wire relay setting. This provides adequate sensitivity on systems where the pilot relays can be set below maximum load currents. However, it appears preferable to trip the remote breaker by a signal sent over the pilot wires in cases where the protective relays are set for larger values.

The conclusion reached by the authors that pilot-wire equipment is competitive with carrier current up to 20 to 35 miles is extremely interesting, because it is a longer distance than generally has been considered the dividing line. The costs upon which these figures are based would be well worth presenting.

The pilot circuit described in this paper meets the problem of induced voltages and station ground potentials entirely by adequate insulation coupled with the fact that these voltages are low in the first place on the particular system under consideration. It would be of interest and value if other papers could be presented describing the experience with pilot-wire equipment on systems where the induced voltages and station ground potentials are larger and must be provided for by suitable protective and drainage equipment. There are many systems where it would be impractical simply to provide the necessary insulation in pilot cables to stand these voltages, and yet on which entirely satisfactory operation is being obtained through the use of proper protective and voltage-gradient equipment, such as neutralizing transformers and insulating transformers.

L. F. Kennedy (General Electric Company, Schenectady, N. Y.): This paper presents a very interesting summary on a large number of pilot-wire-relay installations installed on a single system and presumably planned and maintained by one organization. This paper supplements the relay subcommittee report[1] which was presented last winter and is perhaps more interesting because of the more detailed information which is included.

There are a number of points brought out in this paper, some of which I feel deserve further discussion and some of which perhaps require clarification from the authors.

It is very interesting to me to note that on the loop systems it has been considered adequate to provide backup protection at the source ends of the loop and at the middle only. This means that, in case of a failure of some of the protective equipment to operate correctly, service will be interrupted to approximately half of the customers served by the loop. In some cases we find that very elaborate attempts are made to provide a 100 per cent selective backup system which on this installation would require directional overcurrent relays set as indicated in Figure 1a of the paper. It has never seemed to me that we were justified generally in attempting to provide the 100 per cent selective backup since this seems to be "pyramiding of protection" and naturally brings about the next question as to whether another line of protective equipment should be put in, in case the backup fails. It has always seemed to be much more desirable to select these equipments best suited for a particular job and to plan to keep it in correct operating condition by means of an adequate maintenance schedule. While we all can concede that even with these precautions we will never quite achieve 100 per cent correct operation, it does seem to be good sense to put in a simple backup system which will involve neither too much apparatus nor too much engineering and maintenance to keep it operating correctly. Another factor tending to this thought on my part is the realization that every relay, whether first line or backup, is a potential source of unnecessary breaker operation. Too many backup relays easily may result in conditions whereby undesirable operations occur when there are no faults on the system whatsoever. Further discussion of this point by the authors and others should prove interesting.

From the text of the paper it appears that separate pilot cables of three or five conductors are installed for each line. I would like to ask the authors if this practice is always followed, or do they use a single cable with more conductors when they are protecting the parallel power circuit?

I have been interested for many years in the application of the carrier-current form of pilot protection and hence appreciate any information, based on experience, relative to the minimum length of line at which carrier becomes competitive in cost with pilot wires. The authors have given some information along this line which indicates that carrier-current relaying is very costly.

The authors state that the cost of the pilot wire installed is up to approximately $1,500 per mile. If we use this figure for 20-mile lines as indicated, we come out with an installed cost of the pilot wire of $30,000. This neglects the terminal-relay equipment which admittedly would be very small in comparison with the $30,000 figure.

Carrier-relay prices, including the relays, necessary coupling capacitors, and line traps, and the carrier-current equipment itself, are well known today and span the following range. For simple carrier relaying, such as the type *GMB*, for use on 69 kv has a per-terminal price of about $3,750. The more complete relay equipment including the backup function, such as the type *GCX* combination with carrier, for service at 230 kv has a per-terminal price of about $5,500.

Thus we find that the selling price of the equipment to be purchased for the protection of both ends of a line section is between $7,500 and $11,000. Therefore, it appears that an installed cost of between three and four times the purchase price has been used. It would be of considerable value if other discussers would comment on what installation costs are, as indicated by their experience. It has been fairly common to consider carrier-current relaying as being economically competitive above ten miles, and there have been several installations made for shorter distances. The ten-mile figure would indicate an installed cost of $15,000 or an increase in cost of 50 to 100 per cent over the purchase price. This would seem to be at least adequate for average installations. Further discussion of this point should be of considerable help in determining the proper place of pilot-wire and carrier-current relaying.

In passing, it is in order to add that the carrier-current channel offers possibilities for voice or other communication needs not usually available with pilot wire.

Whatever may be said regarding the comparison between pilot wires and carrier or leased telephone circuits and carrier must, of course, rest upon similar economic comparisons, and local conditions will, of course, change these figures somewhat. The authors state that leased circuits on the basis of $60 per mile per year are competitive with carrier up to 35 miles. Here again the important figure is the one covering the installed cost. Then the comparison may be made against the leased rate and, of course, the leased circuits will be economical for longer distances where the rates are lower.

The relay system used by the authors is one of similarity to those commonly used today with the two-wire a-c pilot system. It is, however, somewhat simpler in its conception, but by reason of these simplifications it appears to have two limitations. I do not believe that with the amount of current which is circulated through the pilot-wire system that it could be applied generally over leased telephone circuits. From the information in the paper it would appear that the normal circulating current at normal load is of the general order of 0.5 amperes. I believe that most leased circuits are restricted to currents of 0.3 ampere or less under fault conditions. Furthermore, because of the fact that the relay system operated on a definite amount of unbalance between the terminal currents, it is necessary that current transformers be chosen which not only "match" but which actually do not saturate materially under the maximum through-fault condition. Current transformers which "match" but which are saturated may differ because of variations in the iron, by more than the amount of current required to cause relay operation. However, this limitation to me does not appear to be a very serious one. I could never see any good reason why it is not thoroughly sensible to require that adequate current transformers be put at the two ends of a series circuit to which differential protection is to be applied.

I was very much interested to note the large number of pilot-wire installations and the very appreciable investment which is represented thereby. When we consider the resistance that is sometimes encountered to even the addition of directional relays where necessary I feel that the authors would be doing a great service if they would amplify their paper by lists of the system benefits which have been realized by this improved protection. The relay engineer is apt to be satisfied that pilot-wire protection is fast and selective and that this is an end in itself. In order to justify an expenditure of several hundred thousand dollars over a period of several years there must be some more benefits obtained than merely the knowledge that the best protection possible is being applied. It must be that this best protection is reflected very definitely in improved performance of the system as a whole or in other kinds of saving made possible by this protection in order to justify economically the continued application of this type of equipment.

In conclusion, I wish to congratulate the authors on an interesting paper and also them and their associates on the excellent performance record of the installed relay protective system.

REFERENCE

1. Pilot-Wire Circuits for Protective Relaying—Experience and Practice, AIEE committee on protective devices. AIEE Transactions, volume 62; 1943, May section, pages 210–14.

T. G. LeClair and **E. L. Michelson:** As Mr. Kennedy points out, the backup relaying used for protection of loops is not 100 per cent selective. The primary pilot relaying clears nearly all cases of trouble. Therefore, it does not seem necessary to require 100 per cent selectivity for backup protection, as the number of service interruptions to customers will be extremely small even when selectivity is not complete.

In all cases separate pilot cables are used for each transmission line. This practice avoids the possibility of incorrect operation on a number of lines for one case of trouble on a pilot-wire cable.

The experience gained with the use of pilot-wire relaying has justified its wide application, because of the benefits gained through rapid and selective clearing of short circuits on the system. This rapid and selective clearing of faults has resulted in improved service and reduction in damage to equipment caused by fault conditions.

In regard to the discussion of relative costs of carrier and pilot-wire relaying, there appear to be three factors which explain the variations brought out in the discussions.

1. Type of carrier system employed affects cost to some extent. For example, some carrier systems include complete backup protection while others do not.

2. The amount of equipment included in the estimate varies considerably. For example, housing for the equipment, additional potential devices, and similar items are included in some cases, while in others they are either not required or not charged to the carrier installation.

3. Construction standards vary with different installations. For example, the number of test switches provided for relays vary in different installations.

The discussion of costs has been very helpful in bringing out the experience of various companies in regard to the cost of carrier installations. In the final analysis, each case must be decided on its own merits, which depend on the individual conditions relating to the installation. It is our opinion that in most cases the selection between pilot-wire relaying and carrier relaying is not made primarily on a cost basis. Carrier relaying is more adaptable for long overhead lines where the attenuation of the carrier signal on the open wire is of reasonable magnitude. Pilot-wire relaying is more adaptable on short underground lines, such as used in large cities where the protection of the pilot circuit is inherent because of underground construction.

In regard to Mr. Sonnemann's discussion, the current transformers and other equipment used for pilot-wire relaying are carefully matched. With regard to the contacts used to obtain remote tripping, in most cases these contacts merely short-circuit the pilot wires, so that the possibility of trouble is minimized. The semiannual tests on pilot cables result in the finding of about one case of trouble on approximately 200 pilot cables in service. This trouble generally is caused by moisture entering a cable joint.

The phase-selector relay that Mr. Leyland suggests is an interesting possibility. However, the additional cost of providing four pilot wires and three relays is very nominal. The external troubles which are experienced with all relays on the high-voltage system represent the large majority of relaying troubles experienced. These troubles include such items as loss of potential on impedance relays due to the blowing of fuses, faults on control cables, and so forth.

In regard to Mr. Sporn's comments on the reliability of pilot wires, it appears that his statements are based on the use of open-wire construction. His comparison would not be valid if the pilot wires are installed either in underground or aerial cable. The authors agree that carrier relaying provides a more reliable form of protection than pilot-wire relaying using open-wire pilots.

Distance Relay Protection for Subtransmission Lines Made Economical

Discussion and authors' closure of paper 43-92 by L. J. Audlin and A. R. vanC. Warrington, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, September section, pages 574–8.

J. H. Oliver (General Electric Company, Philadelphia, Pa.): There has been a continuously growing desire for the past five to ten years for a less expensive distance relay for application to less important transmission lines in the range of 11 to 66 kv. In a few cases the importance of these intermediate-voltage lines has warranted the application of high-grade distance relays, but in most cases other solutions have been employed. In general, relay specifications for these lines have the same requirements as for high-voltage transmission lines, namely, good sensitivity, accuracy, and reliability. However, in most cases it has been felt that the relays can be slightly slower in speed; there does not seem to be the same demand for one-cycle relaying as in the high-voltage transmission lines.

The paper by Audlin and Warrington describes a simple solution to this problem. They have met all the requirements by using a relaying combination employing a distance relay designed for high-voltage transmission lines; incorporating its inherent characteristics of good sensitivity, accuracy, and reliability at the sacrifice of only a few cycles time delay, thus reducing the cost of the relay combination, and thereby making it more attractive for intermediate-voltage lines.

E. L. Michelson (Commonwealth Edison Company, Chicago, Ill.): The authors are to be congratulated on their successful application of the switching arrangement which makes possible the use of one distance relay to protect a three-phase line. While this arrangement is more economical than the use of three distance relays, the increased maintenance work required by complicated relay schemes is an important factor. On a large power system the problems and costs created in maintaining special and complicated relay schemes are significant. In the Commonwealth Edison Company this factor has led to a trend of simplifying the schemes of distance relaying, even though additional equipment may be required. For example, in applying the three-step *GCX* relay we have used two separate ohm units instead of the standard arrangement, which consists of an ohm unit and a transfer switch to make it applicable for two steps.

Eric T. B. Gross (Cornell University, Ithaca, N. Y.): The authors are to be congratulated on their courage for diverting from standard practice and on their successful application of the new relay arrangement. The only practical difference between a set of six standard distance relays (three for line-to-line faults and three for line-to-ground faults) and the new distance relay, controlled in its current and potential connections by a selector relay, is the minimum operating time of five cycles. However, as shorter times are necessary at very high voltages only, the new scheme should deserve a wide field of application. In many cases, the circuit-breaker characteristics necessitate the use of relays with much more minimum time than one cycle, and the new scheme will do as well. Furthermore, the application of the new relay is unlimited for backup protection in connection with carrier-current protection.

A critical analysis reveals that distance relays in the six-relay arrangement may operate incorrectly at certain line-to-line faults, whereas the distance relay in the new scheme is always provided by its selector relay with such currents and voltages that the measurement of the impedance to the fault and the direction of the fault current will be correct; this is another advantage of selector relays.

As I am familiar with the development and application of selector relays and relay schemes in continental Europe, it seems to me that I should report on the experiences which were made abroad. Automatic potential switching was used in Europe since the beginning of the application of distance relays, some 20 years ago. This was possible because the relays were and still are—as we know from the literature—in general, slower than ours. The American standard six-relay scheme has hardly ever been used in Europe. Until about 1929 three distance relays were used and each potential coil automatically provided with line-to-line or line-to-ground voltage, depending on the kind of fault. Later, simpler schemes with two distance relays for all kinds of faults were developed, and finally in 1932 a one-distance selector-relay set for interphase and ground faults. These relay schemes have been applied in overhead-wire and underground-cable systems of the largest capacity and up to 66 kv, and the practical experiences in various European countries (for example, 35-kv overhead system near Warsaw, Poland, or the 35-kv underground-cable system in the city of Budapest, Hungary, and many others) and in other foreign countries (for example, South America) were very satisfactory; the success with the three-relay schemes used since 1923 encouraged the development of cheaper and simpler arrangements which were even more satisfactory from the point of view of exact measurement of the impedance or reactance to the point of the fault.

The work done by the authors is a big step forward as so well expressed in the discussions of Mr. Oliver and Mr. Pratt, the importance of which should be underlined. It is hoped and expected that it will pave the way for a wider application of distance protection in all kinds of systems.

M. H. Pratt (Central New York Power Corporation, Syracuse, N. Y.): It would seem appropriate to discuss briefly a specific problem for which the method outlined in the authors' paper furnishes a better solution than has heretofore been available. This problem occurred on the system of the Central New York Power Corporation.

The 44-kv sub- or area transmission system in the Mohawk Valley east of Utica consists of a double-circuit line mostly on wood poles, extending from Washington Street substation in Utica some 25 miles via East Utica, Ilion, Herkimer, and Little Falls (at each of which points the system supplies a distribution substation) to Inghams. The two circuits were formerly broken and connected through breakers to a bus at Herkimer, the halfway point. East Utica and Little Falls had a tap to each circuit through breakers to the station bus, and Ilion was fed back over a single radial circuit from Herkimer bus. Directional overcurrent phase relays were used with instantaneous overcurrent where possible. It is not pertinent to this brief outline to go into the ground-relay scheme which did not constitute our problem in this case, as one wire-to-ground fault operation was satisfactory.

The troubles with the preceding arrangement were basically three:

1. There was a high percentage of double-circuit multiphase faults during electrical storms which, with the connections outlined, gave frequent interruptions to the substations supplied, particularly East Utica and Little Falls and to a lesser extent, Ilion.
2. The long fault-clearing time on phase faults frequently resulted in conductors being burned down.
3. Good selective settings satisfying all operating conditions were impossible.

The problem was how to correct these conditions at the most reasonable cost. Any attempt to lightning-proof the circuits themselves would have been too expensive. Instantaneous overcurrent attachments, as mentioned, had been applied to the extent that this was feasible. It seemed that a change of connections, looping one circuit into East Utica and the other circuit into Ilion, leaving Herkimer bussed as before, and looping a circuit into Little Falls, was a good arrangement, but it was not possible to make this change and expect any improvement with the existing relay equipment. As a matter of fact, the results from a relay standpoint would have been even worse than before since it would have been even harder to approach proper selective operation under all conditions; furthermore, fault-clearing time would not have been improved. This forced the use of distance relays which were accordingly installed, three *GCX* relays being used at each line-breaker terminal. This, of course, was quite expensive but was still the cheapest possible way in which the problem could be solved, and even this expenditure was justified to correct the trouble. Results were entirely satisfactory.

The ability to apply one distance relay in combination with a phase-selector relay instead of three distance relays as described in the authors' paper gives almost all the advantages of full three-relay distance-relay protection; the only difference is that one-cycle relay operation is not attained, but a minimum time of about five cycles is realized. In any case where this difference is of no practical consequence, the two schemes can be considered to give exactly the same result. In the case of the problem just outlined, five cycles is entirely satisfactory, and it would probably be seldom, with the exception of some major transmission lines, that this would not be true. Such a possible alternate distance-relay scheme is only approximately 50 per cent or roughly $150 more per terminal installation than the widely used directional overcurrent arrangement. It would be seldom that the advantages to be gained by distance relaying if the directional overcurrent scheme is at all unsatisfactory, would not justify this small difference. On the other hand, it takes quite a lot of trouble to justify the great cost differential between directional overcurrent protection and a full three-unit distance-relay installation. The scheme outlined, therefore, truly makes distance-relay protection economical in many cases where formerly it could not have been justified. The new scheme has proved itself in service as entirely satisfactory in every way.

It might be well to point out that the distance relay used with the phase-selector scheme is slightly different in detail design from the standard distance relay. In the case of our installations previously described, where we now have one phase-selector installation, we will gradually replace the standard three distance-relay terminals with the phase-selector scheme as we have occasion to need phase *GCX* relaying on future main high-voltage line installations. It would be helpful to have the relays fully interchangeable for the two types of use.

L. F. Kennedy (General Electric Company, Schenectady, N. Y.): I feel that Audlin and Warrington have given their paper a title which is apt to convey an erroneous impression. To infer that distance relays are uneconomical for circuits in the subtransmission class is to indicate much closer control of costs than has yet been achieved. Many such circuits are 20 miles or more long and may carry several thousand kilovolt amperes of load. The total investment in transformers, line, circuit breaker, and substation required is so great that a few hundred dollars more or less for relays is relatively insignificant. Hence, where distance-relay performance is required, it should be possible to provide it without undue economic restriction.

All this, however, does not mean that it is not worth while to find less expensive ways of obtaining desirable performance. It is certainly true that many circuits in the subtransmission class can be protected adequately with distance relays having a minimum time of five cycles. And it is also true that improved performance can be obtained by the stepped-time distance characteristic in comparison with graded-time overcurrent protection. Hence, what Audlin and Warrington have done is to describe a relay system which is less expensive than the full distance relay and yet has characteristics adequate for a large field of application.

As indicated in the paper the idea of switching a single distance relay is not new. The authors have taken care of many of the objections which had been made to earlier suggestions by always indicating the same distance regardless of the combination of phases involved in the fault and by using a switching sequence which eliminates the possibility of incorrect operation when the number of phases involved in the fault are changed. The laboratory and field tests which have been made provide proof of the over-all correct indication under variable fault conditions.

W. E. Marter (Duquesne Light Company, Pittsburgh, Pa.): This paper describes laboratory tests made on the *GLA* relay in the General Electric Company laboratory. A description of a test made by Duquesne Light Company on the *GLA* relay might be of interest. In the Pittsburgh district silver relay contacts tarnish rapidly, as a result in part of sulphur fumes in the air caused by the combustion of soft coal. An attempt was made to accelerate this action by sprinkling the contacts of an *HGA* current-transfer relay in the *GLA* relay with powdered sulphur. The relay was then allowed to stand for two weeks, and at the end of this period the contacts were covered with a heavy black coating.

Without removing the powdered sulphur or cleaning the contacts, ohm meter readings were made on both front and back contacts, and comparative readings were also taken on clean contacts of a similar relay with the following results:

	Back Contacts	Front Contacts
Tarnished contacts	0.275 ohm	0.225 ohm
Clean contacts	0.010 ohm	0.010 ohm

The contact resistance had increased, but no coating had been formed which could not be broken down by the low d-c voltage impressed by the ohmmeter.

Low a-c voltage was applied to the contacts, and a current of 0.1 ampere was transferred without difficulty.

This test indicates that even under conditions of severe neglect the *GLA* relay can be expected to transfer current circuits satisfactorily.

An installation of *GLA* and *GCX* ground protection has been made on a 22-kv line on the Duquesne Light Company system. The relay was installed in one of two tie lines between 22-kv feeder stations which had previously been relayed with directional overcurrent ground relays. In an attempt to prevent the cross-feed operations the relay was installed at a point which had previously opened on cross-feed for any feeder fault on the adjacent station bus.

About four hours after the relays had been cut in service a ground fault occurred on the 22-kv winding of a transformer bank at the adjacent station. The fault was correctly cleared, and the line on which the reactance ground relay was installed remained in service. The other tie line which was still equipped with directional overcurrent ground protection tripped as usual.

This installation seems to indicate that these relays can be successfully applied to keep subtransmission lines in service which had previously tripped unnecessarily on cross-feed currents.

S. L. Goldsborough (Westinghouse Electric and Manufacturing Company, Newark, N. J.): A suggestion is made in the title that distance-relay protection for low-voltage transmission lines is made economical, but this point is not very well substantiated in the body of the paper. It is not obvious at all that the elimination of two high-speed distance relays and the substitution of ten auxiliary relays containing some 26 odd contacts would result in a combination substantially as economical as the standard directional-overcurrent-relay schemes. The paper admits that this ideal has not been attained but hints that a compromise was made so that the increase in cost over the directional overcurrent relay is only moderate. It would be interesting to know more definitely the degree of economy which has been allegedly obtained. The compilation of this degree of economy should be based not only upon the cost of the relays but upon the cost of maintenance of the 26 auxiliary contacts.

It is well known that one of the most important advantages of the distance-type relay is its ability to ignore changes in generating capacity and operate correctly over a wide range in capacity. Frequently the range of generating capacity is such that the relays are called upon to operate on currents near to or less than the full-load current. It is to be noted that in both the schemes described apparently this quite often very desirable characteristic of the distance-type relay is eliminated because of the type of phase selector used. If the overcurrent phase selectors are used, it is quite obvious that they must be set safely above full-load current and, therefore, the distance relay is not allowed to operate for fault currents near to or below full-load current. Also this would apply to the schemes using undervoltage fault detectors where fault current is limited by fault resistance, since any safe undervoltage setting would not allow the relays to respond. It appears that for ground faults the scheme described will only work when the system is impedance grounded. Whether or not the elimination of the feature of operation on faults of approximately load magnitude is worth while is a question.

Whether overcurrent or undervoltage phase-selector relays are employed, the degree of sensitivity is definitely limited. This means that in a good many cases the equipment would not be able to respond to ground faults containing even a moderate amount of fault resistance. This is especially true in the case of solidly grounded systems which would generally require the overcurrent type of phase selector. It is obvious that in the case of low generating capacity, where the fault current may be only moderately higher than the load current, it would not require very much fault resistance to reduce the ground current below the sensitivity of the scheme.

The ground-relay selector scheme utilizing undervoltage relays would seem to operate correctly on impedance-grounded systems, for here is a definite drop in voltage on the faulted phase and a definite rise in the unfaulted phases, thus not requiring an undervoltage setting very close to normal voltage. However, on solidly grounded systems, using either overcurrent or undervoltage, it is anticipated that trouble may be experienced because of the flow of fault current on the unfaulted phases during single phase-to-ground faults. On systems having several sources of power, either all of them grounded or only one of them grounded, there is the possibility that all three phase wires will carry fault current on single-phase grounds. It can frequently happen that the fault current on the unfaulted phases is as large as the fault current on the faulted phase. Under this condition, all three current phase-selector elements would pickup and lock out, thus preventing the distance relay from functioning.

The same condition definitely would also be encountered with the phase-relay selection scheme since it is possible to have fault magnitude of current on all three phases during two-phase-to-ground faults. Since the scheme must be made such that the distance relay is connected to one set of phase current and potentials for three-phase faults, which occasions the operation of the three overcurrent selector elements, it follows that if three selector elements are operated on two-phase-to-ground faults, the distance relay on two of the possible two-phase-to-ground combinations will be connected to the wrong set of voltage and current.

L. J. Audlin and **A. R. vanC. Warrington:** In his discussion Doctor Gross brought out several interesting points not mentioned in the paper. The success of foreign applications over many years is very encouraging. As regards application to carrier relays, in the present carrier schemes, the directional element of the distance relay is used for both carrier and backup tripping. It would be possible to separate the carrier circuit from the backup circuit by using the distance relays for backup only and, for this purpose, one distance relay with a selector relay usually would be adequate for the backup.

Mr. Marter's data on the behavior of the switching contacts when heavily corroded by contact with sulphur was very encouraging and confirmed our opinion that the switching of current circuits presents no hazard with overlapping high-pressure contacts.

Mr. Goldsborough points out there are 26 contacts in the selector relay; hence it is more complex. In typical impedance and reactance relays there are 12 to 16 contacts per relay. Hence the selector relay has no more contacts than the two relays it is replacing. The contacts of the selector relay, however, have much more pressure than those of the protective relays, because they always have full voltage across their coils whereas the protective relays may be near the balance point.

The number of contacts in any case has little to do with the reliability of the device. The distance-carrier relay equipment has 50 to 60 contacts but is still well thought of. The real factor is contact pressure. All the relay units described in this paper have exceptionally high contact pressure.

The preceding comments also apply to Mr. Michelson's preference for two ohm units in the GCX relay. We believe that relays with high-pressure contacts require less maintenance and are more reliable than protective relays; hence it is advantageous to replace the protective relays by simple auxiliary relays where possible.

Mr. Goldsborough is correct in saying that overcurrent selectors limit the scheme to faults above load current. The first paragraph of the paper explained that these relays were applicable to subtransmission lines hitherto protected by directional overcurrent relays, which obviously must be set above load current. For applications outside this category the standard arrangement of three distance relays is recommended.

We are indebted to Mr. Pratt for providing a good illustration of a typical application for these relays and to all the discussers for providing so many helpful comments.

A letter has just been received from the Electricity Commission of South Africa which contains the following sentence: "We have had nearly ten years of experience with the original *GAX* relays and are very satisfied with their performance so that the soundness of the principles of switching potential and current has been amply demonstrated for us in the field."

Radio-Noise Filters Applied to Aircraft

Discussion and authors' closure of paper 43-132 by C. W. Frick and S. W. Zimmerman, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, September section, pages 590–5.

R. H. Kaufmann (General Electric Company, Schenectady, N. Y.): It is only the final over-all result that is significant in judging the performance of a particular system of radio-interference suppression. Quiet operation of radio receiving equipment is the objective. The presence of radio-interference voltages on electric circuits is not in itself objectionable and becomes so only if these voltages are communicated to the input circuits of radio receivers.

In all-metal planes, the metallic surfaces comprising the ship skin and sectionalizing walls constitute electrical conducting surfaces which may be used to good advantage in the solution of a radio-interference problem. These metallic surfaces possess the following useful qualities:

1. The exterior metallic skin tremendously reduces external radiation of radio-frequency energy from internal electric circuits containing radio-interference voltages and thus minimizes the induction of interference voltages directly in the radio antenna.

2. In the form of bulkheads, the metallic surfaces constitute an effective stop against radiant radio interference. (Insulated electrical conductors which pass through such barriers provide channels on which radio-interference voltages may be transferred to the opposite side of the bulkhead.)

3. Since electrical conductors possessing radio-interference voltages are run in close proximity to metallic surfaces of the plane, the radio-frequency energy is to a large extent concentrated in the small pace between the conductor and the metallic surface, and radio-interference field strength is elsewhere diminished.

Emphasis should be given to the full utilization of beneficial qualities of the metallic surfaces comprising the plane structure. Full advantage should be taken of the possible shielding effect of metal surfaces in screening the lead-in wire to the receiver from surrounding power-circuit conductors. Power-circuit-conductor runs should be kept as far removed from the radio receiving equipment as is practical, and preferably run in close proximity to metallic surfaces, particularly in the immediate vicinity of radio receiving equipment. As has been pointed out in the paper, the importance of a short direct connection between the receiver chassis and a neutral plane of radio-interference voltage, represented by the main body of the ship structure, cannot be overemphasized.

Consideration should next be given to the application of high-quality high-reduction filters effectively placed in power feeder circuits to the radio receiver. Consideration should next be given to effective filter application in nearby circuits and equipment until the desired noise-reduction ratio at the receiver is obtained. It is significant to note that a few high-quality high-reduction filters employed in the vicinity of radio receiving equipment may well produce far more noise reduction than an equal weight of smaller less effective filters scattered throughout the electrical system.

In summary:

1. The suppression of radio-interference voltage is of no significance unless such voltage were otherwise being communicated to the radio-receiver input circuit.

2. The inherent shielding effect of metallic surfaces should be utilized to best possible advantage in diminishing radio-interference voltages delivered to radio receiving equipment, as beneficial results so obtained involve no extra weight or additional devices.

3. It should be borne in mind that a few high-quality high-reduction filters properly applied in the immediate vicinity of radio receiving equipment may be far more effective than an equal-weight investment in numerous filters distributed throughout the ship.

F. O. Stebbins (General Electric Company, Schenectady, N. Y.): The excellent paper by Frick and Zimmerman describes the radio-noise problem on aircraft, one method for attacking this problem, and the final solution that was obtained. The important feature of this solution is the development of a line of small compact lightweight filters for the suppression of radio noise on aircraft.

Aircraft fall, in general, into three classifications as regards the radio-noise problem:

1 There is the all-metal airplane in which the enclosing structure is entirely of metal and the radio antenna (except for the antenna lead-in wire) is well shielded from radio noise developed inside of the airplane structure.

2. There is the airplane in which the enclosing structure is only partially of metal. With these aircraft, the nonmetallic portion covers the upper part of the structure and since the radio antenna is generally located above the airplane structure, the antenna is not well shielded from radio noise developed inside the structure.

3. With the third type of airplane the enclosing structure is entirely nonmetallic, and the radio antenna is again not well shielded from the radio noise inside the structure.

Since one important feature controlling the performance of radio equipment on the airplane is the amount of radio noise on the airplane antenna, the radio-noise problem should be most easily solved on the all-metal aircraft. In many cases the all-metal aircraft is equipped with a larger quantity of airplane accessories which develop high radio-noise levels, and these may counteract to a large extent the beneficial effect of shielding on the antenna.

The specifications covering the permissible radio noise developed by aircraft accessories are generally loose or in many instances do not exist. As a result, the same design of accessory built by two different manufacturers may develop radio-noise levels which differ by as much as 20 to 1 or more. Likewise, the same design of radio receiver built by two different manufacturers may differ widely in sensitivity to radio noise. As the result, radio-noise suppression methods which give successful radio communication on one airplane may fail completely to give successful radio communication on a second airplane of the same design. The radio-noise problem on aircraft has been solved only when successful radio communication could be maintained in flight. It has been found in certain radio-noise investigations that tests had to be carried forward on six or more airplanes of the same design simultaneously in order to be assured that the solution to the radio-noise problem which was obtained would apply to the entire production of that design. If the airplane manufacturers would specify to the accessory manufacturers the radio-noise levels which the accessory must meet, the radio-noise problem would be greatly simplified.

C. W. Frick and S. W. Zimmerman: The authors appreciate the discussions submitted by Mr. Kaufmann and Mr. Stebbins and are pleased to note that each gives consideration to interference control through both design of aircraft wiring and the use of high-quality filters to localize the sources of noise. Both are necessary.

As explained in the introduction, our paper deals with the control of radio interference through the reducing or suppressing of radio-influence voltage in the power circuit. This is accomplished by means of filters which serve to localize the effects of radio-influence voltages at their source. This method of interference control can be employed readily in cases where other methods such as shielding and aircraft wiring arrangements cannot be used successfully. It can be easily applied since a line of lightweight filters of high-noise-reduction ratio is available for ready application.

Aircraft Contactors

Discussion and authors' closure of paper 43-131 by F. J. Russell and A. P. Charbonneau, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, September section, pages 563–6.

M. J. DeLerno (General Electric Company, Schenectady, N. Y.): Two factors which were mentioned in the introduction of the authors' paper are quite important in aircraft contactor design. These are:

1. Changes in barometric pressure from 30 inches of mercury at sea level to about 5.54 inches at 40,000 feet.

2. Ambient temperatures from −50 to 200 degrees Fahrenheit.

The first factor, which is the more important, has a pronounced effect on the arc-interrupting ability of a contactor. Contact tips, which may satisfactorily interrupt a circuit at sea level, may fail at 40,000 feet. Observations on arcs at sea level and at low pressures seem to show that the contact area of the arc with the tips increases as the pressure is decreased. This effect is not noticeable at pressures slightly below atmospheric but becomes appreciable at pressures corresponding to 45,000 feet. It has been observed also that the color of the arc changes somewhat at the lower pressures. A possible explanation for these observations may be that the arc characteristics possessed by the discharge at sea level change to glow characteristics at low pressures, and the transition stage from one to the other occurs at pressures around five inches of mercury or at 42,000 feet elevation. It would be interesting to know if the authors have observed similar phenomena, and if they have found that tip surface area affects arc interruption to a greater extent at 40,000 feet than at sea level.

A second effect brought about by the reduction in pressure at high altitudes is the decrease in the high-potential-breakdown voltages between live parts on a contactor. The AIEE Standard specifies sufficient clearance between live parts to withstand a potential equal to twice-rated volts plus 1,000 volts. Curves showing the air-gap breakdown potential between sphere gaps and between sharp edges indicate that for a given gap the breakdown potential at sea level is from three to four times as great as at 45,000 feet. Similarly, this ratio for creepage breakdown along the surface of an insulating material ranges from about 2.5 to 4, depending on the material.

The reduced pressure of the air at high elevations decreases the heat dissipation of the magnet coil and the contact tips because of the lowered convection coefficient. The component of the heat dissipation caused by radiation is unaffected by pressure changes. However, the total lowered dissipation at high altitudes is offset by the lowered temperatures present at these altitudes.

F. J. Russell and **A. P. Charbonneau:** Mr. DeLerno's observations agree substantially with our own experience. Tests under reduced pressure equivalent to that obtained at altitudes up to 55,000 feet indicate greater tendency for arcs to persist with the arc taking on the characteristics of glow discharges at the lower pressures.

The tendency toward glow discharge reduces the breakdown voltage between live parts through air or across the surface of insulating materials.

These factors are taken into consideration in the design of the aircraft contactors, and the results are verified by actual tests at pressures corresponding to the maximum altitude at which the apparatus is to be used.

Arc-Furnace Control by Regulex Exciters

Discussion and authors' closure of paper 43-95 by T. B. Montgomery and T. H. Bloodworth, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 650–4.

C. E. Valentine (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): It is stated in Montgomery's and Bloodworth's paper, "For a number of years prior to the present urgency for the production of high-quality steel, the development in control methods for electric-arc furnaces remained substantially stationary in this country." So far as the fundamental principle of measurement is concerned, this is true, but control methods include considerably more. As a matter of fact the fundamental method of measurement of the quantity to be controlled is the same as that which the Westinghouse company developed and has used successfully for some 20 years. This is the comparison or balance between the current flowing in the furnace electrode and the voltage between the electrode and the shell of the furnace. The circuits and apparatus, however, by means of which this principle of measurement is used and applied to the regulation of the furnace, have not remained at all stationary. Several years ago the master-electrode motor-control switch was made a part of the standard regulator equipment. This switch and its circuits provide for simultaneous rapid lowering and withdrawal of the electrodes, thus saving time at the beginning and completion of the melting operation. At the same time improvement was made in the arrangement of panel-mounted apparatus and also in detailed parts, such as the reversing switches. These now are spring-mounted in place of pin and sleeve bearings, thus wear and parts which ultimately require replacement are eliminated. More recently development has been started and is still progressing to provide spring and contact assemblies having longer life and requiring less maintenance.

With regard to circuit improvements, high-speed withdrawal of the electrodes in case of a cave-in has been provided on several installations. This is accomplished by means of an overcurrent relay and auxiliary contactor which increase the speed of the electrode motor. This minimizes the duration of high-current surges on the supply system and also lessens outages and loss of time which might otherwise occur because of tripping of the furnace breaker. The use of an electrically operated tap changer is another important improvement which has contributed to saving time in the operation of the furnace.

Reference is made by the authors to the important items of "sensitivity" and "stability." In general, any regulating device which is relatively insensitive requires no special stabilizing or antihunting means. However, as the sensitivity of a regulating device is increased, and, if high-speed action is secured at the same time, some special means of stabilizing is required usually. The balanced-beam contact-making arc-furnace regulator can be set readily for maximum or for minimum sensitivity by means of the contact gaps. Rotating-type regulating equipment can be made with proper design features to have a similar sensitivity, but the maximum sensitivity is fixed by the design of the regulating machine. It is our experience that, if maximum sensitivity and speed of operation of the rotating-type regulator are to be secured, some means for stabilizing it is required. It would be of interest to have the authors explain by what means the Regulex exciter is stabilized when it is adjusted for maximum sensitivity.

Having both types of regulators available, we have made tests comparing the performance of the balanced-beam type with the Rototrol on the same furnace, switching from one regulator to the other. In these tests the standard-design balanced-beam-type regulator was used with no special circuits for fast withdrawal of the electrode under cave-in conditions. In fact it was used as found after operation for about one year. As a result of these tests we found a slight over-all saving in time, when the rotating-type regulator was used, because of fewer interruptions of the a-c circuit by tripping of the furnace breaker. On the other hand, the rotating-type regulator showed little or no improvement over the balanced-beam-type during the refining period. Otherwise the performance of the two types of equipment was very much the same.

To summarize, either the balanced-beam constant-control voltage-type regulator or the Rototrol variable-voltage-type regulator can be used with assurance of satisfactory results. With regard to maintenance, the cost of reversing switch contacts and the time to replace them is very small. In the rotating-type regulator, commutator and brush maintenance take the place of contact maintenance. Should trouble develop in the rotating-type regulator or generator, the cost in money and time required for repair or replacement would be much greater than replacement of contacts on the older-type equipment. The over-all cost of maintenance remains to be determined on the basis of several years of service.

It should be noted that the machines and auxiliary equipment which comprise the rotating-type regulator require inherently more material and cost than the balanced-beam-type regulator. When initial cost and cost of maintenance are considered, it may be found that each type of equipment has its place, depending on the requirements of a given application.

G. E. Shaad (nonmember, General Electric Company, Schenectady, N. Y.): The paper by Montgomery and Bloodworth presents data on arc-furnace control by rotating exciters used as amplifiers of small control indications.

Amplification and speed of response are the two fundamental characteristics of any amplifier. Another type of rotating exciter successfully used in arc-furnace control and other applications gives an interesting emphasis to the range of amplification ratios and speeds of response which can be obtained in machines of this type.

The Amplidyne compresses two full-size exciters into one and gives two-stage performance by the use of armature excitation achieved by short-circuiting the output of the first stage and adding two additional brushes to collect the output of the second stage.

Amplifications of the order of 10,000/1 or higher and speeds of response of the order of one tenth of a second or less can be obtained. Worthy of note is the feature that leakage flux emanating from the armature gives added speed to the Amplidyne, since this flux closes through the air where it can establish itself "with the greatest of ease" and yet can produce useful voltage.

What is the specific value of this faster speed of response and greater amplification ratio in the control of arc furnaces? They mean the ability to respond to smaller departures from the ideal conditions and the ability to respond to faster and shorter changes throughout the furnace cycle. They mean the reduction of the number of machines required in the motor generator set as illustrated in Figure 6 of the paper, since the Amplidyne exciter is at the same time the Amplidyne generator, so that one machine gives two-stage performance.

The results obtained in arc-furnace operation with the Amplidyne control have been remarkable. In one installation the time per heat was cut ten per cent; at the same time the power consumption per ton was reduced.

B. M. Jones (Duquesne Light Company, Pittsburgh, Pa.): I was very interested in the Montgomery and Bloodworth paper. Although we have fair controls on electric-arc furnaces now, compared with what they were a number of years ago, we as engineers and technicians should not be satisfied and should be exploring means of improvement.

In this particular case I believe that the objective is to strike the arc, hold it, and never let it go out; thus, the time to melt down and, hence the cost would be reduced. This also will increase production.

I understand that some of these regulators have been in service for a year or two and have been giving satisfactory performance, and I hope the good work of improving them continues. As a power-company man, I do not care how large the load, so long as it is steady. If it is a fluctuating load I am concerned, even though it may not be large. Therefore, any attempt to steady the load certainly is interesting, for with this kind of load we can provide smaller lines at less expense to the customer than for a fluctuating load which will require a larger line.

E. W. Henderson (English Electric Company of Canada, Limited, St. Catharines, Ont., Can.): Arc-furnace control, as presented in papers by T. B. Montgomery and T. H. Bloodworth and by R. A. Geiselman, C. C. Levy, and W. R. Harris, is concerned with the action of the furnace itself or the circuit between the furnace transformers and the arc furnaces.

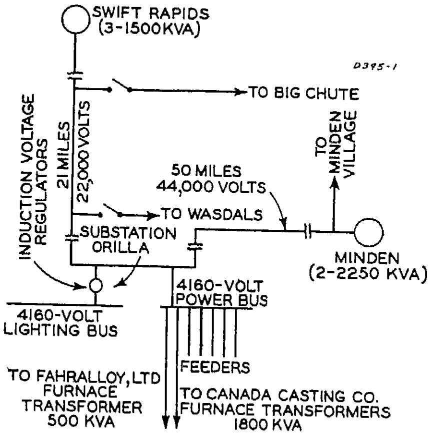


Figure 1

I would like to present a few oscillograms to show how arc-furnace transients affect a power-supply circuit and how in one case the effect of these transients on the lighting circuits was controlled.

The use of a synchronous condenser in parallel with an arc-furnace load is not new; howbeit, the number of installations are few, and these few are installations of fairly large units, for example, 10,000 to 20,000 kva. The system I shall describe is rather small, and the arc-furnace load formed a relatively large proportion of its total capacity. It was located in an Ontario town which has its privately owned utility.

Figure 1 of this discussion shows the original layout, and from this it may be noted that the furnace load of 2,300 kva formed 25.6 per cent of the total kilovolt-amperes of generating equipment, the latter of which totalled 9,000 kva. The power bus was regulated only through the automatic regulators at the generating stations located 50 and 21 miles from the station. The lighting bus was regulated at the distributing station in town by means of automatic motor-operated induction-type regulators. Trouble existed in that these latter regulators were inherently too slow to follow the transients reflected from the arc-furnace loads with consequent objectionable and irritating flicker of varying intensity occurring at irregular and intermittent frequency.

Some idea of the transients which are set up in the power-supply system-feeding arc furnaces may be obtained by an examination of Figure 2 of this discussion. These oscillograms were made with the bottom traces blanked off by a shield mounted within the oscillograph to permit the use of wider trace without interference on the film. The upper trace is that of the voltage variations in the regulated lighting bus caused by the arc-furnace transients shown by the trace immediately under it. The sudden and sustained voltage variations are very marked. Variations in the voltage of the power bus are indicated in the lower trace.

Figure 3 of this discussion shows the circuits as regrouped, with the addition of a 2,000-kva 4,000-volt three-phase 60-cycle 1,200-rpm synchronous condenser in parallel with the furnace loads, and with a buffer reactor inserted between this condenser-arc-furnace circuit and the rest of the system. The condenser was added for several reasons:

(*a*). To control the flicker in the lighting circuits.

(*b*). To improve the power factor of the arc-furnace load and the system as a whole.

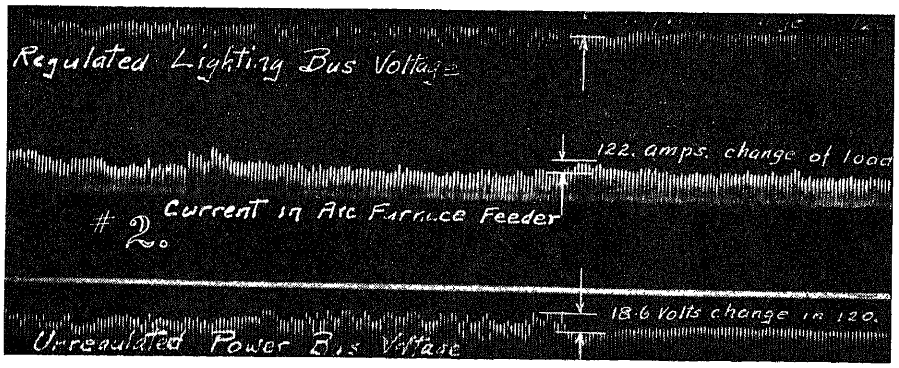


Figure 2

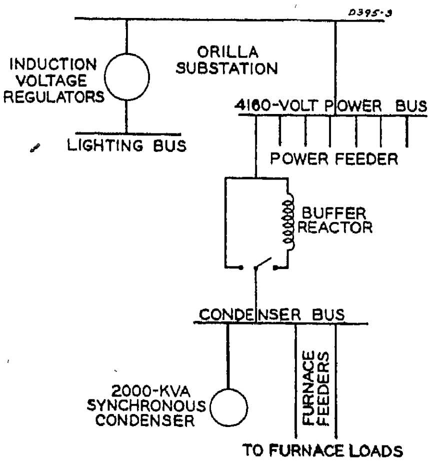


Figure 3

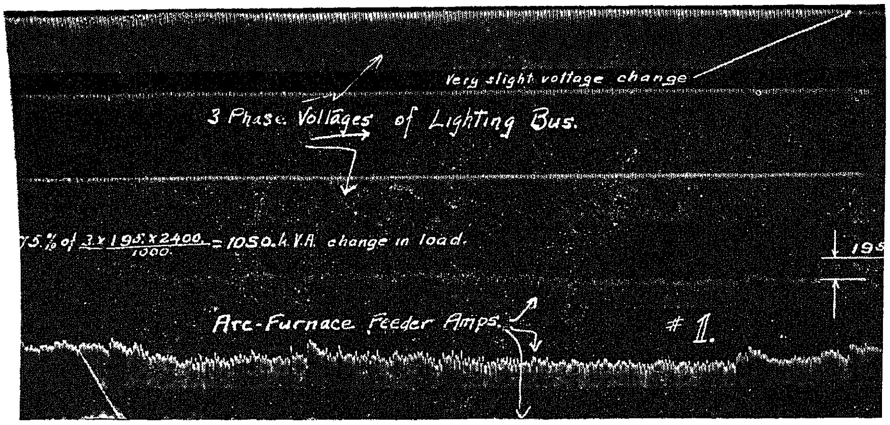


Figure 4 (above)

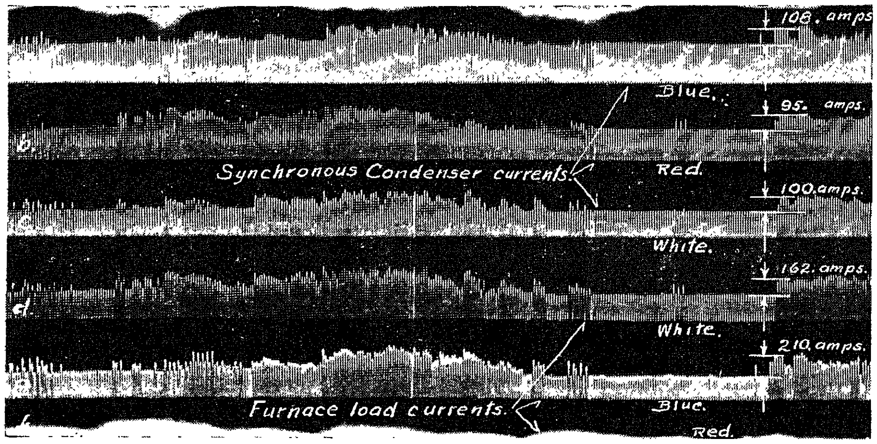


Figure 5 (below)

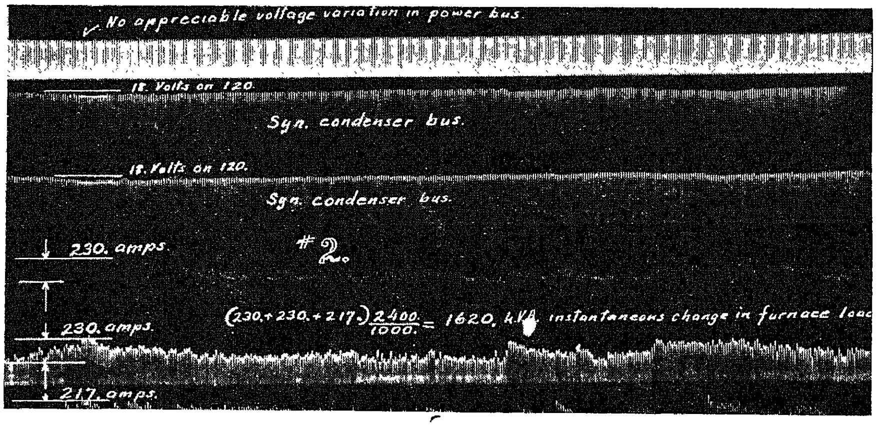


Figure 6

(*c*). To maintain a higher and more steady voltage at the furnaces.

(*d*). To permit additional load to be connected to the system by the power-factor correction.

Figure 4 of this discussion indicates how well the voltage of the lighting bus was controlled. The upper three traces are those of the lighting-bus circuits, showing practically a steady voltage in spite of the transients shown in the lower traces, which are those of the currents in the arc-furnace feeders. The oscillograms were taken simultaneously. It may be noted how steady is the lighting-bus voltage in spite of the arc-furnace transients.

The quick response of the synchronous condenser to transient conditions in the furnace feeders is illustrated in Figure 5. The upper three traces are those of the currents in the synchronous-condenser circuit, and the lower three are those of the currents in the combined arc-furnace load. If we number from the top, 1 corresponds to the same phase as 5, 2 to 6, and 3 to 4. An examination of these pairs will show how rapidly the synchronous-condenser currents respond to those of the arc furnaces. Single cycles are reflected in many cases, and general contour is the same in the different pairs.

The variation in the condenser voltage bus and the steady voltage of the power bus ahead of the reactor under conditions of breaking-down furnace loads are shown in Figure 6 of this discussion. This oscillogram indicates how well the voltage is maintained on the furnaces in spite of rapid and large transients inherent in such arc-furnace operations.

The combined effects of the control of the arc-furnace transients by this method resulted in:

1. Steady voltage with no flicker on the lighting circuits.
2. Steadier voltage on the general power bus.
3. Steadier and higher voltages on the arc-furnace circuits.
4. Extra heats per day on the larger furnace.
5. Reduced time for the heating of annealing furnaces on these arc-furnace circuits.
6. General improvement in power factor and operation of the whole system.

The methods of control by such means as the Rotatrol, Regulex, or Amplidyne may result in improved control of the movement of the electrodes themselves, but any apparatus of this type cannot control the transients in these circuits as their response is inherently too slow. This discussion has been given simply to indicate how rapid and effective these troublesome transients may be and one method of successfully offsetting their reaction on the power-supply system. A synchronous condenser for such purposes must have inherently low transient and subtransient reactances, but otherwise is of normal design and is started and controlled simply and effectively.

T. B. Montgomery and T. H. Bloodworth: The point of stabilization of the control circuit when it is adjusted to maximum sensitivity was raised by Mr. Valentine. The system is inherently more stable than a contact-making system because that response is, for practical purposes at all times proportional to the deviation from normal, for the arc watts. Also, stability is a function of both sensitivity and speed of reponse. In the Regulex system both sensitivity and speed of response are adjustable to suit inertia constants of the furnace. External antihunt means are seldom required. Such means are available but, it was thought, have been used too seldom to warrant inclusion in the paper.

As to first cost and maintenance in comparison with other type of regulators, it appears that over-all power savings will be a determining factor.

The savings in time per heat, as well as power consumption given in Mr. Shaad's discussion, is of particular interest. In the system described the adjustment, both of sensitivity and ratio of amplification, has proved useful, particularly in stability of the system. The use of standard machines is considered of advantage in steel plants and foundries.

With reference to Mr. Jones's discussion, data of system disturbance caused by each, we believe, would be of value by way of comparing the new rotating regulators with the contact-making types. The experience so far emphasizes the fact that, with the new regulators, the arc is not broken normally except to shut down for tap changing or other operations. Since arc interruption causes most disturbance, a steadier load may be expected.

Rotating Regulator for Arc Furnaces

Discussion and authors' closure of paper 43-96 by R. A. Geiselman, C. C. Levy, and W. R. Harris, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 671–4.

B. M. Jones (Duquesne Light Company, Pittsburgh, Pa.): Without casting reflections on the regulator controls that have been in service for a number of years, I believe that Geiselman and Levy have presented data to show considerable advancement in this important method of maintaining a more stable condition in the furnace during the early meltdown period, by holding the arc and preventing its extinction, to a greater extent than has been experienced in the past.

We have taken actual measurements of the kilowatt-hours consumed in a large modern electric furnace with violent frequently interrupted arcing during the early meltdown, as compared with a steady arcing at a somewhat lower tap setting, and found that, under the latter condition, appreciably more kilowatt-hours were consumed, which actually means more heat being put into the melting of the charge. These tests were made three or four years ago, and we have repeated them on other installations since, and these conditions have been confirmed. We also have discussed this matter with others, operators and furnace builders, and all have reached a similar conclusion. From the point of view of putting more heat into the melting of the charge, I believe that the improvements pointed out by this paper today on this subject are a good forward step.

I was struck particularly by the ruggedness of the regulators described by these authors, and I believe that this will lend itself very readily to the replacement of damaged parts in the usual hurry-up steel-mill manner. The complete replacement of the rotating unit with a duplicate is very attractive.

To repeat my statement—strike the arc, hold it, and never let it go out, and we can all melt steel electrically more satisfactorily and cheaply.

G. E. Shaad (nonmember, General Electric Company, Schenectady, N. Y.): Geiselman and Levy have presented an excellent paper of interest to everyone connected with arc-furnace operation. In the past year a good many rotating regulators have been put into operation and have shown excellent results. The system described by the authors will be a welcome addition.

Variable voltage control of electrode motors is not new, since it has been used in Europe for several years. However, the slow response and low amplification of standard d-c generators left these installations with little advantage over magnetic control. The development of the present-day highly sensitive generators with nearly instantaneous response has provided a control showing some remarkable savings in time and power.

To obtain the best results from these regulators we need a high sensitivity and the fastest response it is possible to obtain. As the authors have pointed out, the acceleration characteristics of the electrode motors must be considered, but this limiting value of current should be reached as fast as possible.

In our use of the Amplidyne, with its high amplification built into one machine, we have a unit with a nearly instantaneous response to small regulating impulses. At the same time it has a drooping characteristic that helps prevent injurious overloads to the electrode motor. In this way the electrode motor is not a limitation until large changes in furnace conditions are encountered.

Individual furnaces will have individual characteristics, even though they are the same size and make. It is important that the control be standardized but possess sufficient flexibility, so that it can be adjusted to give maximum performance on any furnace. Stabilization must be provided, so that no hunting can occur, there must be no residual voltage to cause creeping of the electrode motors, and a number of other small items that help save time and increase production.

It might be well to consider the possible savings that may be realized from improved control. On a small furnace producing 25,000 tons of steel a year, these new regulators have shown a saving of 50 kilowatt-hours per ton or with five-mill power a saving of over $6,000 a year. Should we take into account the increased production (ten per cent less time per heat), the figure mounts a good deal higher. Such figures are not proved and probably are exaggerated, but, even though no quantitative data are available, the rotating regulator offers tremendous possibilities.

C. C. Levy (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Claims that have been made for the rotating-type of regulator state that it will result in considerable savings in time per heat. Thus, production is increased if this regulator is compared with balanced-beam or other types of arc-furnace regulators.

Obviously, there are many factors which influence the time of a heat; only those for which the regulator is responsible should be charged against it.

These may be:

1. Frequent automatic interruptions due to inability to clear cave-ins and short circuits without the necessity of a circuit-breaker operation.
2. Inability to hold the arc consistently, making it necessary to restrike very frequently. This might be due to poor adjustment, and it is well known that a poorly adjusted regulator of any type always will increase the meltdown time.

However, tests have shown that the comparative performance of a properly adjusted balanced-beam regulator and a Rototrol arc-furnace regulator show no great difference in the results obtained. Some savings of time, we find, can be credited to the Rototrol because of fewer interruptions during meltdown and smoother operation, but it is only fair to point out that the inherent high speed of the Rototrol during cave-in and short circuit can be obtained also by special field-weakening motor-control schemes with the balanced-beam regulator. The results during the refining period should be quite comparable.

In regard to savings in kilowatt-hours per ton, the theoretical kilowatt-hour required to melt a ton of steel is approximately 335. Now, for medium-size furnace with balanced-beam regulator, we find that to meltdown the charge approximately 400 kilowatt-hours per ton would be a usual figure. To reduce this by 50 kilowatt-hours per ton appears to us to be too close to the theoretical value when radiation losses are considered.

E. W. Henderson: For discussion, see page 981.

R. A. Geiselman (nonmember), **C. C. Levy,** and **W. R. Harris:** As Mr. Levy has indicated, the Rototrol control system is well known and has been applied to a wide variety of industrial applications. Its application as an arc-furnace regulator is one of the latest of these developments. The Rototrol was applied first to an automatic welding head in 1928 and was arranged to maintain a constant-arc voltage, despite fluctuations in the rate of burn-off due to variations in the welding wire and irregularities in the surface to be welded. This application, which may be considered as a miniature arc furnace, was very successful, and, since this first installation, the advantages of the Rototrol control system have led to its introduction in the simplification and better control of many industrial processes. For instance, it has been applied to

1. Radio City elevators.
2. Reversing planer drives.
3. Electric shovels.
4. Mine sweepers.
5. Skip and mine hoists.
6. Bomb spinners.
7. Machine tools—120/1 range.

I would like to accent the simplicity of the Rototrol and its associated circuits. The Rototrol itself is nothing more than a standard d-c machine with several field windings, and it functions entirely through the interaction of these fields. Other than the several field windings it is exactly the same as a standard d-c machine, and any mill electrician familiar with d-c machines can maintain it. An important advantage of the Rototrol is that it contains no low-resistance paths in the armature circuit which may be affected adversely by brush drop.

I would like to point out also that the adjustments made to the Rototrol during installation do not require further attention. These adjustments are made in the field circuits which are static and contain nothing to get out of adjustment.

The rates of response and the practically infinite number of operating speeds provided by the variable-voltage system are suited admirably for application to arc furnaces. A close examination of the oscillograms presented by Mr. Levy in the paper will show that, during the meltdown period where the unbalances may be large, the response is at a high rate and high operating speeds are reached, which results in fast electrode positioning and quick withdrawal in case of a cave-in.

During the refining period, where the unbalances are small, the response is at a slower rate, and the speeds are a fraction of the maximum speed where only small adjustments are required. The oscillograms also show that sudden unsustained variations do not cause electrode movement.

We certainly would agree with Mr. Shaad's remarks on stability.

The factor of stability is very important in any regulated system.

With the Rototrol system the following factors affect stability:

1. Level of ampere turns in the control fields.
2 The degree of self-energization and amplification.
3. The time constants and mutual inductance of the various circuits.
4. Mechanical inertia and friction of the system.

Careful consideration of these factors in the regulator design, coupled with rates of response and electrode speeds proportional to deviation from the regulated value, has resulted in an inherently stable regulator capable of initial adjustment to meet operation best with any type of furnace.

Mr. Henderson is to be complimented on the excellent results he has obtained in eliminating light flicker due to furnace loads. Use of the buffer reactor, synchronous condenser, and associated switchgear, or apparatus of similar nature, is to be expected for problems of this type.

Electronic Control of D-C Motors

Discussion and author's closur of paper 43-144 by K. P. Puchlowski, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, pages 870–7.

G. A. Caldwell (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper gives a very clear and complete explanation of the features that must be added to an electronic rectifier to give it characteristics suitable as a power supply for a d-c motor. By referring to Figure 2 and Figure 8 of the paper, it is apparent that this modern unit has the necessary compensating devices actually to improve the inherent speed-torque characteristics of the motor itself, so that the combination of the rectifier and motor makes it possible to operate the d-c motor over a very wide range in speed with good speed regulation at all speeds.

There are many applications in industry where such a drive is desirable. On many machine tools it has been normal practice to use a constant-speed a-c motor with suitable change gears or a hydraulic mechanism to obtain the necessary speed range. It is also possible to obtain wide speed ranges by using a motor–generator set with a suitable rotating regulator to obtain the necessary speed range and speed regulation.

A majority of the wide-speed-range applications are constant torque in their nature. In other words, the horsepower output of the d-c motor must be proportional to the speed at which it runs. The electronic control described in this paper controls the motor speed by regulating the voltage applied to the motor armature. The motor horsepower output, neglecting the effect of ventilation and wave form, is, therefore, proportional to the speed at which it runs. Hence, the electronic drive is ideal for these constant-torque applications.

Other types of wide-speed-range applications are a combination of constant torque and constant horsepower; that is, the torque required at low speed may be three to four times that required at high speed. By introducing the feature of field control on the d-c motor, which is mentioned in the paper and is a standard feature, the electronic drive also can be applied satisfactorily to this application. The practical speed range that can be obtained by field control is usually considered 4 to 1. If we select a motor that has a base speed of 690 rpm and a maximum speed of 2,760 rpm by field control, the drive gives constant horsepower over this range, and then by using armature voltage control below 690 rpm, the drive can be extended down to approximately 50 rpm at constant torque. A number of such applications have been made, as well as those using motors without field control.

Some wide speed-range drives are almost constant horsepower in nature, which means that the torque requirements of the drive increase in proportion to the decrease in speed. When the speed range is wider than can be obtained by motor field control, then the entire drive must be oversize to obtain sufficient torque at low speeds. Consequently, the drive is larger than necessary for the upper speed range, just as is the case when a motor–generator set is used.

As previously indicated, the same type of characteristics can be obtained with adjustable voltage drives when a motor–generator set with rotating regulator is used, but on certain types of applications the electronic drive has advantages. An important advantage is that the electronic drive has no moving parts and, therefore, is not a source of vibration. This is very important on such applications as grinders, where the surface finish is affected by any source of vibration.

It is also usually simpler to mount the electronic drive than the motor–generator set required in the adjustable-voltage scheme of control.

With the electronic control, the speed-adjusting mechanism is a small potentiometer which is much easier to mount and also to operate than the speed-control rheostat used in the adjustable-voltage scheme of control. On certain machine-tool applications it is desirable to control this speed from a push-button station, and the potentiometer of the electronic scheme can be mounted in the same station as the push buttons. To accomplish similar results with the motor–generator-set scheme of control requires the use of a motor-operated rheostat.

This small potentiometer also is much easier to incorporate into any scheme of control in which the speed must be varied from some mechanical mechanism in going through an automatic sequence of operation.

This form of electronic control promises to be a very useful tool to an industry and an important member of the electronic family.

G. E. Walter and **H. H. Leigh** (General Electric Company, Schenectady, N. Y.): As an ingenious approach to the problem of obtaining accurate and useful control of d-c motors by electronic methods, the circuit arrangement discussed in this paper warrants close study. The author's list of characteristics of present-day rectifier drives is noteworthy, in particular, for its complete and detailed presentation of the advantages to be expected in such systems.

His circuits and the diagrams illustrating their operation contain certain features and apparent discrepancies, however, to which we would like to call attention.

Two of the circuits deserve careful functional scrutiny. Although the description of the IR compensating circuit's operation is quite clear in the text of the paper, its actual performance is rather obscured by Figures 2, 8, and 9 of the paper, in which a startling discrepancy occurs. Figure 2 clearly confirms the author's statement of the d-c motor equation by showing a constant slope of the speed-load line regardless of the no-load speed of the motor. If the same speed is desired at all loads, the motor equation can be rewritten as

$$nc\phi = E - IR = \text{constant}$$

indicating that the terminal voltage of the machine must be increased by an amount equal to the IR drop of the armature circuit, if the speed is to remain unaffected by the load. The parallel lines of Figure 2 of the paper show distinctly that the speed of a certain motor operated from a d-c source will drop by 125 rpm from one ampere to 4.3 amperes armature current at any setting of no-load speed.

Figure 8, with its flat speed-load lines, creates the impression that the author's electronic drive automatically increases the terminal voltage on the motor by an amount very nearly equal to the internal IR drop at any load. Since the IR drop depends only on armature current, the curves of Figure 8 should be produced by increases in terminal voltage which are the same from zero to full load at each speed. In other words, the compounding of a d-c motor with constant field should be independent of speed so long as armature reaction effects are negligible.

However, Figure 9, purporting to display the actual voltage characteristics that caused the flat speed-torque curves of Figure 8, claims that such curves can occur with voltage increases ranging from 13 volts at 90 rpm to 4 volts at 1,000 rpm. Our experience and test records on thyratron motor control indicate that the voltage characteristics of Figure 9 could not produce the speed characteristics of Figure 8. It is interesting to speculate on the possible disposition of this internal IR drop, particularly at 1,400 rpm, where a perfectly flat characteristic in Figure 8 is caused apparently by no increase at all in the voltage of Figure 9.

The current-limiting circuit is the other circuit deserving special attention. It operates fundamentally by placing on $R2$ a voltage signal in addition to the one already present as an indication of motor speed. Tube 3, therefore, responds to a grid voltage which is the algebraic sum of the signals from the motor armature, the current-limit circuit, the IR compensating circuit, and the setting of the speed-control potentiometer $P1$. To retard the firing point of the armature thyratrons, and thus limit the current, the signal on $R2$ produced by tube 5 (in the current-limit circuit) must increase at least at the same rate as the signal on $R2$, caused by the armature voltage, is decreasing. The only possible way for this to occur is for the armature current itself to increase at the same time.

In addition, the effect of the IR compensating circuit is to oppose the effect of the current-limiting circuit. Although the latter eventually overwhelms all its opposition, the net result is shown in Figure 11, where the current-limit circuit becomes operative at four or five amperes but allows eight amperes to flow when the motor is stalled. Between these limits the armature current is a complicated and subtle function of motor speed and the settings of four potentiometers—$P1$, $P2$, $P4$, and $P6$. A change in any one of these will affect the armature current. There is, as a result, no sharp limit to motor current and torque.

The particular effect of speed on the armature current shown in Figure 11 of the paper, where the current varies over a 2 to 1 range as the motor speed increases from zero to its preset value, also presents an interesting contrast with Figure 12. In this latter set of curves the armature current is constant allegedly for an appreciable period while the motor is accelerating; yet Figure 11 clearly states that if the speed changes while the current-limit circuit is operating, the current also will change. Obviously, Figure 12, to be true, must be based on a constant current limit independent of speed, which Figure 11 shows is not produced by the author's circuits.

Finally, although the paper mentions automatically controlled reversals, it does not describe the operation of the current-limit circuit for negative speeds. This is the situation confronting the electronic circuits at the instant the motor armature is reversed by contactors and reconnected to the rectifier. Study of the current-limit circuit discloses that it should become almost useless under this condition, since the signal from the armature voltage on $R2$ actually opposes the signal on $R2$ from the current-limit circuit. Instead, the "anti-plugging" feature mentioned by the author may play an important part in the automatically controlled reversals to which he refers, conceivably in decelerating by dynamic braking or a similar method.

K. P. Puchlowski: The remarks of Mr. Walter and Mr. Leigh concerning the alleged discrepancy in characteristics shown in Figures 2, 8, and 9 of my paper seem to indicate that the authors of the discussion have analyzed the subject from a very general and conventional angle only. In fact, it may be stated that no discrepancy whatever exists if careful scrutiny is applied to the whole phenomenon. Obviously, in any drive the IR drop of the motor will be a function of load current only, and there is no need, therefore, to speculate on its disposition at different speeds of the motor. The authors of discussion, however, have overlooked the very important factor of armature reaction.

It is stated plainly in the paper that Figure 2 is a graphical illustration of the equation

$$n=\frac{E-IR}{c\phi} \tag{1}$$

under the following assumptions: E = constant, R = constant, and ϕ = constant; whereas, Figures 8 and 9 represent actual operating characteristics, where the effect of armature reaction is well pronounced.

In fact, equation 1 may be rewritten in the following form:

$$E=Cn\phi+IR \tag{2}$$

For ϕ = constant and n as a parameter equation 2 obviously will represent a family of straight lines parallel to one another, as shown in Figure 2 of the paper. Under actual conditions, however, ϕ is not constant because of the armature reaction but is itself a function of the armature current, $\phi = F(I)$, and equation 2 will become

$$E=CnF(I)+IR \tag{3}$$

Careful analysis of equation 3 will reveal that the armature voltage consists of the sum of two terms, the second of which represents a straight line independent of the speed n. The first term, however, which is also a function of the armature current I, is directly proportional to the speed, and for different speeds the sum of the two terms will represent a family of curves, $E = f(I)$, which are not parallel to one another. The curve, $\phi = F(I)$, has normally a negative slope as compared with the positive slope of the straight line representing the term IR, so that the voltage increase necessary to maintain constant speed will be reduced with respect to the case ϕ = constant. Furthermore, the negative slope of the curve representing the first term of equation 3 will vary with speed, namely, will be greater for higher value of n, and, consequently, the voltage increase necessary to maintain constant speed will be reduced more for higher speed. In other words, the voltage increase will decrease gradually with increasing speed, a phenomenon which can be observed very nicely in experimental curves plotted in Figure 9. Thus, Walter's and Leigh's statement, that voltage characteristics of Figure 9 could not produce the speed characteristics of Figure 8, is incorrect.

Walter's and Leigh's interpretation of the current-limiting circuit seems to be incorrect as well as confusing. The principle of any current-limiting action is based obviously on the fact that, when the rising current attains a definite value, the current-limiting device starts to operate, and its operation results in a more or less sharp decrease of voltage while current is still increasing, so that soon a final limit of current is reached. This current limit would correspond to the maximum possible torque, that is, locked armature with a low voltage applied to it. Hence, the implications that the current-limiting circuit being discussed has a disadvantage consisting of the fact that the armature current must increase during the process of current limitation is surprising.

Besides, the authors of the discussion seem to have overlooked the basic fact plainly stated in the paper that the amplifier consisting of tube 5 and associated circuits is characterized by a sharp cutoff and high gain. When the value of the resistance $R2$ is made sufficiently high, the sharpness of increase of voltage across $R2$ produced by tube 5 and the corresponding sharpness of decrease of armature voltage are high enough to produce a sharp current-limiting action. It is apparent that an infinite sharpness of the speed-current curves at the limit value of current is impossible, since this would be contrary to the principle of continuity of physical curves. In other words, the current-limiting action can be made more or less sharp but never infinitely sharp.

The fact that the action of the IR compensating circuit opposes that of the current-limiting circuit is of no consequence, since, as it is stated plainly in the paper, the ratio of change of the effective voltage produced by tube 5 to that produced by tube 4 is very high.

Thus, the shape of curves shown in Figure 11, and, in particular, the effect of current limitation shown thereon is contrary to what the authors of discussion seem to believe. It is not a result of the inherent properties of the circuit but of the selection of the circuit constants and, in particular, of resistances $R1$ and $R2$.

If the aforementioned is summarized, Walter's and Leigh's criticism concentrated around the sharpness of current limitation and the two particular graphs shown in Figure 11 seems to be immaterial, since, in the first place, such a thing as a "constant current limit independent of speed" does not exist; in the second place, the extent to which the actual conditions will approach this idealized situation has not been specified by the authors of discussion to constitute a reference for comparison; and, in the third place, characteristics shown in Figure 11 do not imply that a higher degree of sharpness of the current limitation cannot be obtained with the current-limiting circuit in Figure 10.

Curves shown in Figure 12 were intended to demonstrate graphically the principle of current limitation, and, rigorously speaking, no current-limiting device ever will produce a strictly "constant" starting current, but the flat top of curves 2 and 3 in Figure 12 always will have a slight slope, so that this so-called by the authors of discussion "constant current limit" will be decreasing actually with increasing time.

During the rapid reversing of contactors, when the rectifier system becomes for a limited time an inverter, and regenerative braking takes place, the voltage across $R2$ produced by the armature voltage of the motor actually changes its polarity and opposes the voltage produced by the current-limiting tube 5, as Walter and Leigh have stated rightly. However, it should be remembered that, although this signal voltage has an opposite sign with respect to the voltage produced across $R2$ by tube 5, the sign of the rate of change of the two voltages is the same, and, therefore, the current-limiting action during the rapid reversing remains fully effective. Thus, Walter's and Leigh's statement that "it should become almost useless" is incorrect.

A New-Type Carrier Relay Protection

Discussion and authors' closure of paper 43-147 by E. M. Wood, H. W. Haberl, and F. A. A. Baily, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, December section, pages 774–9.

M. J. Brown (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The departure from conventional carrier-current schemes described in this paper is interesting as an example of the type of system which can be developed to meet the requirements of a particular power system.

Of special interest is the transmission of carrier at time of fault to *initiate* tripping at the distant end. The transmission problems associated with this condition have made its general use restricted. However, it is used as a means of clearing transformer faults when a transformer is included with a line between a local breaker on the low side of the transformer and a high-voltage breaker at the distant end of the line. Should a fault develop in the transformer, its presence is indicated by differential relays which trip the breaker on the low side of the transformer and transmit coded carrier to a code receiver at the distant end of the line to trip the breaker there A line trap is inserted between the line and the transformer in the phase conductor used for carrier.

It will be an interesting supplement to this paper to have an account of actual performance after operating experience with the system has been acquired. The conditions that appear to be most unfavorable for carrier transmission during fault are as follows:

1. Close-in fault A minimum of coupling between phases will exist if the fault is near the coupling capacitor.
2. Fault at odd quarter-wave-length points on line. A line short-circuited at a quarter-wave-length point offers high impedance at its terminals. Line input current would be reduced, and transfer of energy through magnetic coupling with other phase wires would be reduced.
3. Fault at half-wave-length points on line. A line short-circuited at a half-wave-length point offers low impedance at its terminals. Line input watts would be reduced because of the mismatch, and the transfer of energy to other phase wires would be reduced. At 205 kilocycles the quarter-wave-length points are 1,200 feet apart. In the 20 per cent end zones of the longest line, a distance of 40 miles at each end, these will be a total of 352 quarter-wave-length points.

Offsetting the aforementioned conditions are:

1. That fault resistance will mitigate reflections when trouble occurs at quarter-wave-length points.
2. That the high-transmitter power can produce an appreciable signal at the receiver through considerable attenuation in addition to that offered by the normal line.

The high-transmitter power requirement will limit the application to a very small percentage of installations, because the cost of such equipment will handicap unfavorably its application in competition with the now widely used systems employing transmitters of moderate power output.

T. A. Cramer (General Electric Company, Schenectady, N. Y.): The authors of this paper have described an interesting application of carrier-current equipment which illustrates the flexibility of this tool for power-system engineers, whereas the standard blocking-type carrier equipment is a relay channel with a telephone attachment, this equipment is essentially a telephone channel with a relaying attachment.

I would like to ask the authors if the 400-watt carrier-output figure was determined by the results of the field tests mentioned in the paper?

A modern blocking-type carrier relay equipment has a power output of approximately 15 watts, employs about eight electron tubes, and operates directly from the 60-cell-station storage battery. It is obvious that the equipment described in the paper is operated from an a-c source. Is the station service considered satisfactory for this purpose, or is a motor generator operated from the station battery to supply power to the equipment?

Our specifications for the blocking-type carrier relaying have always contained the requirement that faults fed from only one end of the line section should be tripped at high speed to avoid unnecessary disturbance to the system when a faulted line is closed on. It appears that this specification is not met by the relay arrangement described in this paper. When the line is closed on a fault in the remote end-zone tripping will occur on the second-zone time delay, since the remote relays will not be energized because of the breaker being open at that point.

When high-speed reclosing is applied to a transmission line, it is important that both ends of the line be tripped simultaneously at high speed, and the blocking-type carrier always trips in this manner. With the transferred-trip type of carrier a fault on the coupling phase near the station can block the tripping impulse and will result in one breaker tripping in the second-zone time which usually is too long for a successful reclosure.

F. M. Rives (General Electric Company, Schenectady, N. Y.): Since its introduction approximately ten years ago the use of the blocking-signal-type of carrier relaying has become so widespread that the equipment designs and application technique associated with this system have become well standardized. As a result other possible carrier relay systems have not received the attention they may well deserve. When an art reaches this stage the stimulus of a new approach or new point of view is often very helpful. We are indebted to the authors of this paper for supplying this stimulus.

In the development design and application of carrier channels for relaying one of the fundamental axioms, heretofore rigidly observed, is that the carrier channel shall not be required to transmit the tripping or blocking signal through the line section in fault. In fact the primary purpose of the line traps, invariably used with relay channels, is to prevent any possibility of the fault interfering with the proper transmission of carrier on the sound sections. Here is a system the usefulness of which admittedly depends on the transmission of the carrier signal over the line section in fault. The statement of the paper referring to tests made to determine whether or not the required signal could be transmitted through a fault definitely is inconclusive. It would appear that the success or failure of transmission would depend on several factors, such as the nature and location of the fault and the carrier frequency used, as well as transmitter power. Simply increasing the transmitter power from 15 to 400 watts may not be sufficient to insure transmission; in fact, it is doubtful if several times this power would insure transmission.

It would be of interest to know why the equipment was designed for operation in the frequency range of from 145 to 205 kilocycles. The increase in line attenuation in this range of frequencies as compared with the 50- to 100-kilocycle range on the long sections may be greater than the gain obtained by increasing the transmitter output power from 15 to 400 watts. For example, the line attenuation of the 300-mile line from Leaside to Beauharnois figures to be approximately 14 decibels at 50 kilocycles, 20 decibels at 100 kilocycles, 30 decibels at 150 kilocycles, and 45 decibels at 200 kilocycles on an interphase basis. Phase-to-ground operation may be some 20 per cent higher. The gain of 14.26 decibels obtained by increasing the transmitter power thus is less than would be gained by changing frequency on the 300-mile line from 150 to 50 kilocycles.

From the description of the carrier equipment it would appear that it was designed primarily for the telephone-communication function with the relaying as a secondary function. It would be of interest to know what the operating experience has been with the telephone equipment. Offhand it would seem that the time required for the control functions involving tandem relays would result in an undesirable amount of clipping and confusion for certain users. In the development of standard types of single-frequency-duplex telephone equipment, employing voice-actuated control circuits, it has been found necessary to obtain higher and higher speeds for the control functions with a result that the over-all start time or the interval between the time a sound enters the microphone of the transmitter and the time it emerges from the receiver has been reduced to less than ten milliseconds. This high-speed control has been found necessary to minimize clipping, so that the system will function satisfactorily for all types of voices.

In the conclusions the authors state as one of the advantages provided by their system that only one carrier set is required at each station. While this may be true where the number of lines involving a single carrier channel is relatively small, there is a definite limit to the number of audio-frequency channels that can be accommodated above the voice-frequency band. It would be of interest to know what the authors consider to be the maximum number of audio channels or lines that can be handled on a single carrier-frequency channel. Using a single carrier set for several lines and for joint operation of relaying and communication has the operational disadvantage common to many joint or multiple-use schemes in that service interruptions for tube replacements or for normal maintenance will interrupt all of the functions associated with a single set. It would appear also that the complexity of equipment and perhaps the over-all cost required to provide this joint operation are somewhat greater than would have resulted if separate relaying and communication channels using standard commercially available equipment and conventional blocking-type relaying had been established.

L. F. Kennedy (General Electric Company, Schenectady, N. Y.): The authors' statement "If the carrier should fail . . . in no case would the incorrect operation or failure in the carrier system be detrimental to the relay system . . ." reminds me of the often criticized relay-operation report classification, "correct but undesired." We are interested, however, not in relay operation as planned but relay operation which will maintain service. If the relay operation is correct, but system stability is lost and load interrupted, then we seem to need a new definition of "correct."

The essential difference between the system described and most of the previously installed carrier-relay systems is that in this new system the carrier signal must be transmitted through or around a short circuit, whereas in the past the signal has been transmitted over an ideal carrier channel, namely, a sound section of power line isolated from external influence by a high impedance (line trap).

The paper indicates that carrier-transmission tests were made with one- and two-wire-to-ground faults, and that these were successful, but it fails to indicate what phases were involved in the faults and what phase was used for the carrier transmission. Neither does the paper indicate the fault location for these tests. It hardly seems possible that successful transmission of the tripping signal could be obtained even in the case of a single wire-to-ground fault if the fault were on the coupling phase and close to the transmitter.

The authors have indicated that 0.25- to 0.30-second clearing is necessary for stability in the case of three-phase and two-phase-to-ground faults. But by using a tripping system it would not be obtained for 20 per cent of the three-phase faults and perhaps for 13.3 per cent of the two-phase-to-ground faults (two thirds of the 20 per cent end zone).

In the paper by A. H. Frampton and E. M. Woods the percentage of three-phase faults occurring on this system is given as 15 and the percentage of two-phase-to-ground faults as 26. Thus it would appear probable that the high-speed action would not be obtained on 6.6 per cent of all faults. However, since the high-speed action is required only for multiphase faults (in this case 41 per cent), we find that of these faults 16 per cent may fail to give the desired action. Hence, even if every relay element and carrier element operates correctly we can never achieve 100 per cent correct operation from a system viewpoint.

With modern carrier installations from about 1937 or 1938 there is no evidence that the blocking-type of carrier relaying has failed because of transmission difficulties. If the operating record with the new system should show that the tripping signal can be transmitted successfully through all kinds of faults, then we would have data which would show general parity with the blocking system.

If we concede that the transmission of the signal is of equal reliability, then we could compare the relative merits of the blocking system with fast but possibly incorrect tripping to the tripping system with correct but delayed tripping in case of random failure. There scarcely seems to be enough advantage in favor of the tripping system to justify the complications of a 400-watt in stead of a 15-watt transmitter and a somewhat greater number of relay units.

Since there seem to be grounds for reasonable doubt as to the reliability of the carrier-tripping channel when most needed, it would be sensible to await more data showing the operating experience and particularly the record showing the success of the transmission with all kinds of faults near the transmitters. If the data indicate successful performance on this score, we then can determine properly whether the greater complications are justified.

REFERENCE

1. THE 220,000-VOLT SYSTEM OF THE HYDRO-ELECTRIC POWER COMMISSION OF ONTARIO—II, A. H. Frampton, E. M. Wood. AIEE TRANSACTIONS, volume 60, 1941, pages 1215–21.

E. M. Wood, H. W. Haberl, and **F. A. A. Baily:** The authors thank the various contributors to the discussion. Their comments were informative and, we believe, to the point.

The following, in part, answers the questions raised.

As stated in the paper, we had primarily a communication problem and also a desire to improve the relaying time.

In trying out the trip-transfer scheme for relaying we had two points in mind.

The transfer-trip scheme, if it would work, was considered more sound fundamentally in that failure to transmit the carrier impulse does not cause incorrect tripping; it merely fails to give the desired improvement.

We have the disadvantage that we transmit our transfer-trip signal over the faulted line. However, on the job in question we have alternate route lines over which we can transmit the transfer-trip signal as shown in Figure 2 of our paper, and, although we have made tests to prove that the carrier signal will pass through grounded transmission circuits, it is not essential, as the carrier signal can operate through the other two sides of the triangle.

With the blocking scheme using low-power carrier sets, we doubted the reliability of transfer across the very long lines; failure to transmit actually would allow incorrect tripping. There also is a problem in contact racing inherent in the blocking schemes. Our own experience with that type on another line during the past season indicates that this is a real problem.

The frequency range assigned in Canada for power-line carrier, except for low-power sets, is 145–205 kilocycles. A basic design of high-power transmitter receiver in this range suitable for application of tones being available was used.

With the present available design the upper limit of the audio-frequency circuits without distortion is roughly 6,000 cycles, and to avoid using tones which might have second harmonics the number of usable tones with the present filter system has been designed so as not to exceed three. This is in no way to be taken as the maximum number of separate tones which may be used on some future job which would be designed specially for this purpose. With adequate filter design and adequate transformer design the number of tones is limited only by the harmonic content of the carrier system and the sharpness of the cutoff of the tone-acceptor circuits.

At some future time it might be possible to design a carrier set to operate in the 50–100-kilocycle range and superimpose on this carrier audio-frequency tones up to 10,000 cycles, which set would have a minimum of side-band cutting in the higher ranges.

We had hoped before this closure to make some tests on tone transmission through a line with faults of various types, as nearly as we can determine, at the worst location near a terminal. Operating conditions have caused these to be postponed.

The quality and reliability of voice communication through all kinds of weather have been first class; there is some speech clipping which a few of the users do not like, but through constant usage most of the objectors have found it to be not too unsatisfactory. Naturally, refinements in speech delay could be an added feature, and this would eliminate entirely the first syllable clipping.

The speed of operation is suitable for our system.

Power at 60 cycles for the set is supplied in some cases from existing separate 60-cycle-station services and in others from ten-kilowatt flywheel-type motor-generator sets driven from the 25-cycle-station service. Where the latter is used there is a stand-by supply for low-power communication, only from a smaller set fed from the station battery.

Dethermalizing Arc Quenchers

Discussion and authors' closure of paper 43-142 by Lloyd F. Hunt and Fred H. Cole, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 720–4.

H. E. Strang (General Electric Company, Philadelphia, Pa.): The authors of this paper are to be congratulated for presenting to the industry this record of logical and orderly development in connection with circuit-interrupting devices, particularly since it is supplemented by such interesting field-test and field-service data.

This contribution is noteworthy in that it provides additional recognition of the mobility of oil and the effectiveness of the movement of such a mass of cool oil across arcing contacts. This fundamental principle has already been discussed, and it is now evident that the desirable end result may be accomplished by a variety of physical arrangements of contacts and other parts.

Test performance and field experience seem to speak well for the designs discussed by the authors. However, it is doubtful if the typical drooping characteristic of arcing time against current justifies the unqualified assumption that "breaker performance will improve at higher interrupting capacities."

W. M. Leeds and **A. W. Hill** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): It is with some surprise that we learn in this paper that a method has been found for interrupting arcs under oil with the formation of no carbon whatsoever! Our experience has been that there is a fairly close correlation between the amount of free carbon liberated and the energy dissipated in the arc.

It is stated in the paper that common operating practice requires the inspection of an oil circuit breaker after each automatic operation. Although this may be a justified procedure for high-voltage circuit breakers of very early design, there are many modern circuit breakers in service which are not looked into until after 10, 20, or in some cases as many as 50 automatic operations.

The dethermalizer assembly in an insulating tube, as illustrated in Figure 6 of the paper, shows the stationary contacts bridged by horizontal insulating surfaces. Have the authors noted any tendency for carbon deposits on these surfaces to affect the dielectric strength of the insulation?

In the companion paper by F. H. Cole

and C. P. Garman it is shown that the opening of the middle pressure-generating contact one-half cycle in advance of the end contacts gives a definite improvement in performance. Was this used on the 230-kv dethermalizer design tested at the Laguna Bell substation? Figure 7 of the paper would indicate that the contacts all parted simultaneously. It would be interesting to know the reason for grading the width of the ports, starting with small openings at the top. Since the heavy currents are interrupted with shorter contact separation, it would seem that the grading should be in the reverse direction so as to allow expansion for the heavier pressure surges and higher-velocity oil flow through the first few channels.

Lloyd F. Hunt and **Fred H. Cole**: It is gratifying to have such good discussion on our paper. Mr. Leeds and Mr. Hill bring out the point that carbon is formed by arcing; however, in our tests, after many short-circuit interruptions, there was no measurable amount of free carbon caused by the low arc energy. In their second paragraph it is possible for us to talk only about things we have had experience with, which include 110 230-kv oil circuit breakers of many makes. It has been necessary to give almost all of these switches a great deal of maintenance. In fact, as a result of many short-circuit clearances, the switches have not been in operating condition until they are repaired. We are happy to state that the development during the last year will allow us to fit up our switches with modern arc quenchers that will allow the large number of operations. With reference to their third paragraph, we have stressed the insulation of the dethermalizers by repeated line-dropping tests and have found no evidence of deterioration of the dielectric strength of the insulation. In their fourth paragraph they ask about the parting of contacts. The three contacts in each dethermalizer part at the same time. Also, it has been shown by Leeds' work, as well as by these tests, that a certain contact separation is necessary at arc extinction to prevent restrikes; therefore, it is important not to force all the oil out of the ports before this separation occurs. If the top ports were too large this would occur, and good arc extinction would not result. The pressures and amount of oil flow have been calculated for this correct operation, and field tests have substantiated the calculations.

In answering Mr. Strang's discussion, we wish to agree with him that the movement of cool oil across the arcing contacts is fundamental for good arc extinction. When we said "Breaker performance will improve at higher interrupting capacities," it was intended to mean within the rating of the switch. If the switch is rated at 500,000-kva interruption duty, it means that the performance of the breaker improves up to that point. This is necessary, as the design is such that pressures developed in the quencher are controlled by the ports and are so arranged to give most effective arc extinction at that rating with the proper factor of safety. Should the switch be rated at 2,500,000-kva interrupting capacity, the ports would be so designed that the maximum arc extinction would occur at this value and also be designed to withstand the internal pressures with the usual factor of safety. This is why the dethermalizing arc quenchers improve in operation up to the duty for which the switch is designed.

We appreciate the interest shown in our paper and thank those who took part in the discussion.

A New One-Cycle Directional Overcurrent Relay

Discussion and author's closure of paper 43-143 by W. C. Morris, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 725–7.

S. L. Goldsborough and **S. C. Leyland** (Westinghouse Electric and Manufacturing Company, Newark, N. J.): There is an implication made in this paper that high-speed directional overcurrent relays with satisfactory contact co-ordination have not been available in the past. This is entirely incorrect. Relays of this type have been used for years for phase protection, ground protection, and in several carrier-current schemes, and have had a service record requiring no apologies.

The prime requirement for correct contact co-ordination in any high-speed relay is the use of elements which inherently have co-ordinated time characteristics. For many years correct contact co-ordination has been obtained between a high-speed loop-type directional element and a balanced beam-type impedance element in the impedance relay. By the proper use of the beam construction for the overcurrent element in a directional overcurrent relay using a high-speed loop-type directional element, correct contact co-ordination is obtained. This combination has been used in a directional overcurrent carrier-ground relay with complete success for the last six years. Conventional applications of a similar relay have been made also. Directional overcurrent relays utilizing a plunger-type overcurrent unit and a loop-type directional relay also have a highly satisfactory operating record. This type of relay has been available for at least ten years.

The author states that "For the protection of most of the lower-voltage lines, distance relaying has generally been considered to be too expensive." Yet, in this relay he uses the same elements that are used in some distance-type relays. A single-zone distance-type relay need have no more elements than the author's single-zone overcurrent relay. Such a distance relay has the added advantage of operating for the same distance when wide variations in system generation (magnitude of fault currents) are encountered. In evaluating cost of protection, it is necessary to consider all factors involved. A relay which will provide better protection under all system conditions certainly has more value than one whose effectiveness varies with changes in generating capacity.

In the synopsis Mr. Morris mentions the common practice of adding instantaneous trip attachments to directional overcurrent relays but bemoans the many incorrect tripouts. Apparently he has encountered many questionable applications of instantaneous trip attachments. This certainly has not been our experience, for the instantaneous trip attachment, when properly applied, has an operating record as high as any other type of protective relay or relay scheme. It is hoped that the statement in the paper will not discourage the use of instantaneous trip attachments. One reads further with the anticipation that this application problem, if it really is a problem, is to be solved within the simplicity and price class of the common directional overcurrent relay but finds only one more version of a relay type which has been in service for years.

The only new feature which the author has introduced in his paper is directional control of a high-speed relay, and directional control solves only half of the problem. It is still necessary to have elements which will co-ordinate inherently for the other half of the problem, and such elements have been in common use for many years.

C. L. Smith (Rochester Gas and Electric Corporation, Rochester, N. Y.): The relay described by Mr. Morris is a welcome surprise to me. Not so many years ago large numbers of induction-disk directional overcurrent relays were installed throughout the utility industry. The directional elements were characterized as "instantaneous," and then, after the first severe fault either hung on too long or failed to clear at all, we woke up to the fact that the inverse time–current characteristic of the overcurrent element was being nullified neatly and completely by the inverse-voltage characteristic of the directional element. The addition of a directional-control feature did not improve the situation much; it just cascaded the over-all time of both elements. In due time, faster and better directional elements were developed, and, when they were used with high-speed overcurrent elements, they provided high-speed directional overcurrent protection at normal voltage and reasonably fast operation at low voltage. Contact co-ordination became a difficult problem. In many cases the only solution was the use of auxiliary interlocking or short time-delay relays. These remedies were more or less effective but did nothing to cure the fundamental trouble. We still had overcurrent elements that speeded up and directional elements that slowed down as the severity of the fault increased.

Apparently in this relay complete co-ordination has been accomplished by eliminating this basic difference in performance of the two elements. At the same time, performance at low voltage has been improved greatly. If my memory is correct this is the first time that any designer has submitted time–current curves and oscillograms of single-phase directional-element performance based on one per cent of normal voltage. It would be interesting to see similar curves based on normal voltage.

I should like to ask Mr. Morris what provision has been made for current calibration of the overcurrent unit; also, does he contemplate developing a similar relay having a three-phase directional unit co-ordinated with three single-phase overcurrent units?

G. B. Dodds (Duquesne Light Company, Pittsburgh, Pa.): The relay described in the paper undoubtedly will fulfill the requirements where an instantaneous overcurrent power directional relay is required. The number of places where it can be used can be determined only by experience.

The widespread use of instantaneous overcurrent elements for the first zone protection of transmission circuits has come about by the ease and little expense of installing instantaneous overcurrent devices in the cases along with the time-delay and directional elements. There are a great many locations on systems where coordination between the instantaneous overcurrent elements and the directional element is not required, because the fault current in the direction desired to be tripped by the instantaneous overcurrent element is greater than that in the opposite direction. Of course this condition can exist on only one side of a station. Undoubtedly, many applications have been made where coordination should be required and is not obtained, and the new relay will be applicable in such places.

The question arises, however, if, with a directional element that is separate from that used for time-delay protection on a circuit, and with an overcurrent element that, along with the directional element, requires a separate relay case from that used for time-delay protection, a further step might not be taken and an instantaneous directional impedance relay be used at little or no greater cost than the directional overcurrent relay described. Use of the impedance protection would avoid entirely any variation in operation due to variation in fault current caused by generation capacity or by system connections.

A further thought is that, if the relay described in this paper is used for instantaneous overcurrent protection of a circuit in place of distance relays, a variation obtained in fault current will occur, depending on whether the fault is single-phase or three-phase. As a great many faults are single-phase short circuits rather than three-phase short circuits, could the difference in measurement be eliminated by adding a second-phase current to the instantaneous overcurrent element as is done in the ohm unit of the reactance relay to obtain the same current under both fault conditions?

Information on the relay burden and on the available current pickup taps would be of interest.

The relay described undoubtedly will prove a valuable addition to the existing protective relays, although its field of wide application is not immediately apparent.

G. A. Powell (New York Power and Light Corporation, Albany, N. Y.) and **A. J. McConnell** (General Electric Company, Schenectady, N. Y.): The problem of coordination of the contacts of instantaneous overcurrent and high-speed directional relays is an old one which has not received proper attention heretofore. Mr. Morris describes a relay which solves the problem in a manner that eliminates the application effort normally occasioned by the possibility of an adverse contact race. Once the need for an instantaneous overcurrent relay is established, the application work is simply the determination of the overcurrent setting.

The New York Power and Light Corporation has successfully circumvented this problem by the use of a polyphase directional relay with polyphase-voltage restraint in conjunction with high drop-out plunger-type instantaneous overcurrent relays. The same directional relay is used with the time-delay overcurrent relays.

The voltage restraint eliminates the contact race caused by load current, both at the inception of the fault and during the opening of the adjacent circuit breaker. The contact race occasioned by reversal of short-circuit current is also eliminated if, after the nearby circuit breaker opens, the impedance around to the fault is greater than the impedance setting or reach of the directional relay. Any infeeds between the relay and the fault serve to increase the apparent impedance at the relay location.

It is granted that the polyphase directional relay with voltage restraint entails more application work than does the relay described by Mr. Morris. In addition to determining that the directional relay will not operate on the current reversal, it is necessary to check that the reach of the directional relay is sufficient during a phase-to-phase fault. It must be borne in mind that a polyphase directional relay with voltage restraint has approximately twice the reach during a three-phase fault that it has during a phase-to-phase fault. However, it has been determined that, if the impedance setting is less than twice the line impedance from the relay around to the fault (after the nearby circuit breaker has opened), the contact race is avoided. This generally provides adequate margin for desired operation during a phase-to-phase fault. If not calculation will usually indicate that the impedance setting can be increased considerably and still avoid the contact race.

F. S. Benson (Pacific Gas and Electric Company, San Francisco, Calif.): A high-speed one-cycle directional overcurrent relay that has positive operation when the fault is in the nontripping position will be of great benefit to relay engineers, as it has been necessary in the past to prevent incorrect operation of such a relay by the installation of other types of relays to provide a restraining action. The use of capacitors in the watt element to produce a voltage for one cycle after the circuit voltage has fallen to a very low value during a short circuit is indeed a clever idea and should provide the necessary torque to insure correct operation of the watt element.

The arrangement of a current phase-shift circuit, the poles of which are magnetically coupled in the overcurrent element to prevent its operation until the auxiliary contact in the watt element is closed, should provide the necessary time delay to prevent the overcurrent-element contacts from closing until the power flow is in the correct direction. This arrangement should prevent effectively the race between the contacts of the watt element and the overcurrent element from closing indiscriminately and should insure a correct sequence of contact closing.

It would be interesting to know the type of contacts used, whether or not a definite pressure is required, before sufficient current flows to operate the holding coil, whether or not they have ample wiping action, or whether they are of a nonbouncing design. The relay test man and inspector who must set and service these relays in the field would prefer to have adjustments that could be made to the relay in the field, such as changing the location of the core with relation to the magnetic field and some provision for changing the magnitude of the current through the phase-shifting arrangement of the overcurrent element. Since the overcurrent element (one-half cycle) may be closed instantaneously, it seems that taps should provide a range similar to that used with instantaneous overcurrent relays. No mention of such taps is made in the paper.

A description of the relay assembly would be quite timely, particularly the method of mounting the assembly within the case. It would be advantageous to be able to remove the complete assembly from the case when the relay is mounted on a switchboard, thereby making it much easier to make minor repairs or complete inspection. No mention is made with regard to relay test equipment, whether or not that will be included within the case, or whether the customer must provide his own relay-testing assembly.

W. C. Morris: Mr. Goldsborough and Mr. Leyland state that relays of this type have been available for years and have a service record requiring no apologies. The good service record is easily understood, if one stops to figure out the applications which are allowed after all the limitations, given in their current handbook section covering the relay they refer to, are imposed. The only applications which are allowed are those in which the load current cannot close the directional-unit contacts, and fault current can only close the directional-unit contacts when the fault is in the protected section. These limitations apply to single lines, loop circuits, or parallel lines.

Another limitation imposed by the handbook section is that the pickup of the overcurrent unit must be set "above the maximum instantaneous asymmetrical fault current for a fault at the next bus with maximum connected capacity." This limitation means that, on the usual system with the X/R ratio equal to 10, the instantaneous peak after one-half cycle is 1.74 times the peak of the symmetrical wave; therefore, an overcurrent element set for 90 per cent of the line length on an asymmetrical current basis would cover a much smaller portion of the line on a symmetrical current basis.

As the induction-cylinder element operates on the a-c component, it is not necessary to set its pickup on the asymmetrical fault-current basis, but it can be set to protect 90 per cent of the line, regardless of whether the fault current is symmetrical or asymmetrical.

We do not believe that operation in connection with carrier-current equipment is comparable, because there is a time delay of the order of a cycle during which the elements have time to position themselves correctly. We believe the diverse viewpoints shown by different discussers merely prove again how long unsatisfactory operation can be experienced before the reasons are found and the faults eliminated. It is admitted that distance relays have added advantages, but the cost of a distance relay is considerably more than that of an instantaneous directional overcurrent relay.

We too hope that the use of instantaneous trip attachments will not be discouraged in places where they can be applied properly. As a general rule, they can be applied everywhere with overcurrent relays, but we caution against using them in directional overcurrent relays.

In the closing paragraph Goldsborough and Leyland state that the only new feature of this relay is directional control of a high-speed relay. Directional control of a high-speed relay is not new, as instantaneous plunger relays have been controlled directionally by short-circuiting their operating coil. The new feature of the relay is the proper co-ordination of the contacts which allows the relay to be applied anywhere, even if load current is in the direction to close the directional-unit contacts.

Mr. McConnell has very kindly provided the information and method of determining when voltage-restrained directional relays can be used with high-speed instantaneous overcurrent relays, and, since we believe it adds materially to the subject, we are including it.

CO-ORDINATION OF INSTANTANEOUS OVERCURRENT RELAYS WITH A POLYPHASE DIRECTIONAL RELAY WITH VOLTAGE RESTRAINT

Assume instantaneous overcurrent relay at R set at maximum current for fault at X_1. This is when line Z_D is out and with Z_B a minimum

$$I_{R\ \max}=\frac{1}{Z_B\ \min+Z_C}$$

With both lines in and a fault at X_2

$$I_R=\frac{Z_D}{Z_C+Z_D}\ \frac{1}{Z_A+\dfrac{Z_CZ_D}{Z_C+Z_D}}$$

$$=\frac{Z_D}{Z_A(Z_C+Z_D)+Z_CZ_D}$$

and R will operate if $I > I_R$ max or when

$$\frac{Z_D}{Z_A(Z_C+Z_D)+Z_CZ_D}=\frac{1}{Z_B\ \min+Z_C}$$

$$Z_D(Z_B\min+Z_C)=Z_A(Z_C+Z_D)+Z_CZ_D$$

$$Z_DZ_B\min=Z_A(Z_C+Z_D)$$

$$\frac{Z_DZ_B\min}{Z_C+Z_D}=Z_A$$

$$\frac{Z_DZ_B\min}{Z_C+Z_D}=KZ_A$$

where

$$K=1 \qquad (1)$$

With breaker at X_2 open and a three-phase fault

$$E_R=I_R'Z_C+I_DZ_D$$

and the apparent impedance viewed from $R(Z_R)$ is

$$Z_R=\frac{E_R}{I_R'}=Z_C+Z_D\frac{I_D}{I_R'}$$

but $I_R'=\dfrac{Z_A}{Z_A+Z_C+Z_B}I_D$

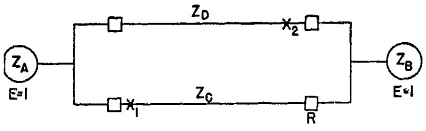


Figure 1

or $Z_R=Z_C+\dfrac{Z_D}{Z_A}(Z_A+Z_C+Z_B)$

$$Z_R=Z_C+Z_D+\frac{Z_D}{Z_A}(Z_C+Z_B) \qquad (2)$$

Z_A is substituted from 1 in 2

$$Z_R=Z_C+Z_D+\frac{Z_D(Z_C+Z_B)(Z_C+Z_D)K}{Z_DZ_B\min}$$

$$Z_R=(Z_C+Z_D)\left(1+\frac{K(Z_B+Z_C)}{Z_B\ \min}\right)$$

$$Z_R=(Z_C+Z_D)\left(\frac{Z_B\min+KZ_B+KZ_C}{Z_B\ \min}\right)$$

Insofar as Z_B and K are concerned, Z_R appears to be a minimum when Z_B is a minimum $(=Z_B)$min and when $K=1$ (minimum relevant value for K, since the overcurrent relay would not operate in equation 1 for $K<1$).

Minimum

$$Z_R=(Z_0+Z_D)\left(2+\frac{Z_C}{Z_B}\min\right) \qquad (3)$$

which means that the directional relay would not operate if set for less than the Z_R of equation 3.

We believe that Mr. Smith's comments represent more nearly the true operating experience with adding instantaneous attachments, and we are grateful for his contribution, particularly in commenting on this being the first time information on one per cent of normal voltage has been presented. We hope to continue this practice and regret that we do not have time to supply him with similar curves on normal voltage at which, of course, operation is many times faster. Mr. Smith and other discussers asked for information on how the current pickup was adjustable. This is done by loosening a clamp on a collar which carries the spring and then by rotating the collar. A four-to-one adjustment range is provided, and it is possible to get any intermediate value desired. A three-phase directional unit co-ordinated with three single-phase overcurrent units, about which he inquired, has not been drawn up, but if there is a demand we should be glad to undertake this development.

We thank Mr. Benson for his comments. In answer to his question, the contacts used are our nonbounce billiard-ball type. No definite contact pressure is required to make contact, as in any induction relay, and as soon as trip current flows the holding coils assure adequate contact pressure so that no burning can occur. The core of the relays can be adjusted in the field, but it is not expected that this ever will be required. Incidentally, a smooth core is used in these relays, and it is the intention of the design to require no adjustment. Mr. Benson's request for a description of the relay case assembly can be answered simply by stating that the relay is mounted in our standard drawout case; we believe this satisfies all the requirements mentioned in his last paragraph, including testing facilities.

Mr. Dodds brings out a good point in that this relay is in the class immediately below one-step distance protection. We have not thought that it would be worth adding double windings to overcome the 15 per cent difference in measurement for three-phase and phase-to-phase faults, but this can be done easily. We appreciate the suggestion. He asked about the relay burden which is of the order of 0.8 ohm (20 volt-amperes) for a 4-to-16-ampere relay, and for a ground relay of one-half-to-two-amperes rating it is approximately 50 ohms.

Aircraft Transformers—Small and Light

Discussion of paper 43-146 by O. Kiltie, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, pages 899–902.

J. E. Yarmack (Federal Telephone and Radio Corporation, East Newark, N. J.); **E. C. Wentz** and **C. C. Horstman** (both of Westinghouse Electric and Manufacturing Company, Sharon, Pa.): Mr. Kiltie's paper on aircraft transformers is indeed a valuable contribution to the electrical art serving the aviation industry. With the advent of 400-cycle power system there is no doubt that there will be more and more requirements for aircraft transformers light in weight and small in size.

Our respective companies have collaborated in the design and manufacture of several aircraft transformers, and the results have been very gratifying. They concern the three-phase 400-cycle air-blast-cooled units used in the rectified a-c–d-c power plants generating from 200-ampere to 800-ampere 28.5-volt output.

The weights of these transformers are illustrated in Figure 1 of this discussion. Curve A is a typical weight characteristic of the three-phase transformers employing the Hipersil cores. By working these transformers up to 300 degrees Fahrenheit temperature, the weight could be reduced considerably. The conservatively established relation, however, is as shown by

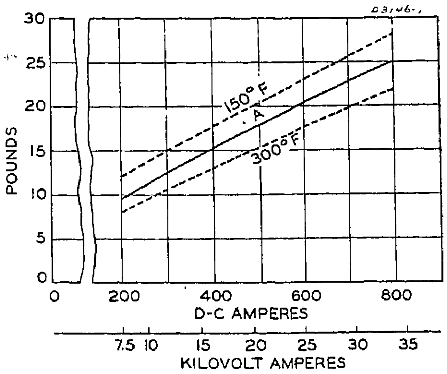


Figure 1. Weight relation of the aircraft transformer to the kilovolt-ampere rating and the rectified d-c output

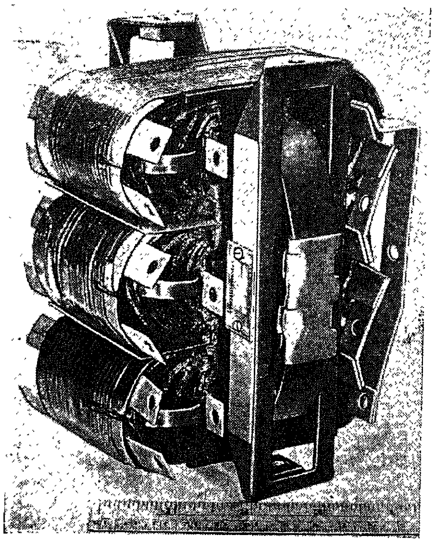


Figure 2. 7.5-kva three-phase 400-cycle air-blast-cooled aircraft transformer

curve *A*. One should not overlook the efficiency merits of these transformers as related to the output rating. With the 35-kva transformer a 97 per cent efficiency is readily obtainable; whereas, the 7.5-kva transformer will give an efficiency in the neighborhood of 92 per cent. Figure 2 of the discussion illustrates the view of a 7.5-kva 400-cycle three-phase transformer employing seven-mil Hipersil core. Its volume is 125 cubic inches and weight is 9½ pounds. With forced-air ventilation to the amount of 250 cubic feet per minute, its maximum temperature rise is in the neighborhood of 85 degrees centigrade.

Mr. Kiltie's statement in reference to the transformer-rectifier unit attracts our attention in particular. He states that the development work is well advanced on self-regulating transformer-rectifier units of 3-kw and 6-kw ratings. He also states that the 6-kw rating unit is within 40 pounds weight, and that very close regulation is accomplished by a control reactor, three-phase high-temperature transformer, variable resistor, and selenium rectifier.

We have explored various means towards the design and construction of the aircraft unit in question. The system that Federal Telephone and Radio Corporation has developed recently is the varying input at the primary of the transformer. This adjustment is accomplished by means of electronic regulator and the associated mechanism. The rectified output voltage is compared with the standard one. If the output voltage is higher than the predetermined value, the difference between this and the standard one affects the adjustment in the primary of the transformer and thus reduces the input into the rectifier. With the low output voltage the process is reversed. Figure 3 of this discussion illustrates the complete unit. It essentially consists of Hipersil-core transformer and autotransformer, selenium rectifier, cooling system, and electronic voltage regulator with its component parts. The equipment is housed in a sturdy lightweight case. The complete unit weighs approximately 41 pounds. The equipment operates from a 200-volt 400-cycle three-phase power supply. The output voltage can be selected anywhere between 26 and 30 volts and is held constant within plus or minus three tenths of a volt at any output current from zero to 200 amperes.

Parallel Operation of Airplane Alternators

Discussion and author's closure of paper 43-149 by D. W. Exner, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, December section, pages 755–60.

Cyril G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Mr. Exner's paper discusses what is probably the most important single consideration in the use of a-c systems for aircraft. As pointed out by him, there are three important factors to be considered:

1. Characteristics of the prime mover.
2. Characteristics of the control system.
3. Design characteristics of the alternator.

Since alternator design plays such an important role, it is in order to point out some of the lessons learned from the design of the 12.5-kva alternator of Figure 2 of the paper and the 40-kva alternator illustrated in Figure 6 of the paper.

The specifications for the 12.5-kva alternator imposed two requirements not conducive to minimum weight of active electrical parts. Rated speed was fixed at 3,430 rpm, and appreciable flywheel effect was demanded. When the 40-kva alternator was requested, the rated speed was fixed at 6,000 rpm. Moreover, the alternator designer was given practically a free rein in the choice of the most favorable proportions without regard to flywheel effect A comparison of the results achieved is startling. The 40-kva alternator develops three times as much output per pound of active material, that is, copper and magnetic material, as its predecessor. (In this comparison, the output of the older machine was taken as 12.5 kva of load plus the excitation power.) The improvement in output per pound, of course, is due partly to the increased speed. But even after correction is made for the difference in speeds, the 40-kva alternator delivers nearly twice the output per pound per revolution. This important gain is due to a number of factors:

1. Absence of the flywheel-effect restriction.
2. Increase in rating.
3. Progress in design art.

These comparisons teach an important lesson to those who specify the alternators as well as to those who design them.

Perhaps even more remarkable is the output per pound of total weight of the alternator. If we exclude the weight of the exciter (which is an accessory) the weight of the alternator is less than one pound per horsepower of output at the five-minute rating. Even when the weight of the exciter is included, the weight is only of the order of one pound per horsepower. These figures compare very favorably with similar figures for the main engines.

W. K. Boice and L. G. Levoy (General Electric Company, Schenectady, N. Y.): This paper presents an excellent discussion of problems which are part of the a-c power-supply program initiated by the Army Air Forces and which are now receiving very active attention from the major electrical manufacturers.

A low short-circuit ratio indicates that the reactances of the machine are relatively high. If one of the engines driving paralleled alternators accelerates suddenly, the alternator driven by this engine will accelerate also, until the governing system can restore normal alternator speed. During this time the alternators will swing apart in angle, and an electrical torque, called the synchronizing torque, will resist the increase in angle. The value of this torque is dependent upon the reactances, particularly the transient reactance of the machine, and is comparatively low in the case of a machine with relatively high reactances. Because of time lags in the excitation system, the exciter will not be able to increase the alternator excitation quickly enough to offset the effect of higher reactances during the initial stages of the disturbance, and, therefore, higher displacement angles will result for a given disturbance. Hence, a machine with a low short-circuit ratio, being less able to resist sudden changes in angle, will have, in general, less transient stability than a machine with a higher short-circuit ratio. The ability of a machine to remain in synchronism during other momentary disturbances, such as electrical faults, is similarly affected by the short-circuit ratio.

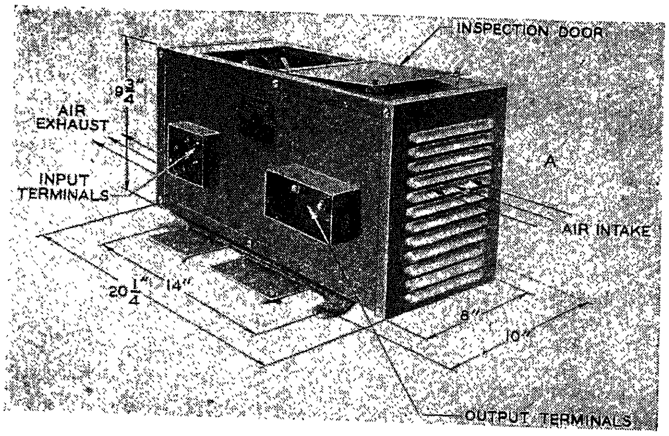


Figure 3. Aircraft transformer – rectifier unit of 6-kw rating. The unit weighs 41 pounds and is 20 inches long, $7^3/_4$ inches wide, and $9^1/_2$ inches high. It contains transformer with the primary winding arranged for variable input, cooling system, selenium rectifier, and electronic regulator

As Mr. Exner points out, alternators with a short-circuit ratio of 0.60 have been used successfully when driven by auxiliary engines. Experience is yet to be obtained on the effect of the relatively poorer transient-stability characteristics of such alternators when driven by variable ratio transmissions from main engines. The paper emphasizes that the high acceleration rates of these engines accentuates the problem of maintaining synchronism between paralleled alternators.

While the maximum engine accelerations impose severe requirements on constant speed drives, governors, and alternators, data now available indicate that accelerations normally encountered will be materially less than the maximum possible values.

The choice of the method of synchronizing to be used on any airplanes must take into account the weight and complexity of equipment, as well as the extent of the operator's duties.

Parallel operation has certain disadvantages as well as the advantages outlined in the paper. These include the following:

1. Additional control equipment required.
2. More severe requirements for governor performance.
3. Routine attention given by operator to balancing of loads.
4. The fact that a disturbance to a paralleled a-c system disturbs the entire electric system on the airplane.
5. Larger fault currents, requiring larger and heavier circuit breakers.

It is questionable whether or not parallel operation is essential even for combat planes, although it is desirable from an operating standpoint. A-c systems should take into account possible nonparallel operation, even of systems normally to be paralleled.

Mr. Exner mentions the savings in wiring weight obtained at higher voltages. The effect of system voltage on weight of power-distribution-system copper is illustrated by the following figures, based on the same power, transmission distances, and current density.

27.5 volts direct current with ground return, 100 per cent.

120 volts direct current with ground return, 22.9 per cent.

120 volts direct current two-wire, 45.8 per cent.

120/208 volts, three-wire, three-phase alternating current, 0.75 power factor, 30.5 per cent.

These figures demonstrate why aircraft equipment for higher voltages, both a-c and d-c, have been developed.

D. W. Exner: Boice and Levoy state that a low short-circuit ratio indicates that the reactances of the machine are relatively high. Apparently, they have based this somewhat broad statement on experience with the more conventional 60-cycle designs. Actually, a lightweight 400-cycle design which has a lower short-circuit ratio and a considerably higher direct-axis synchronous reactance X_d has a transient reactance X_d' approximately one half as great as the values commonly existing in 60-cycle machines. Hence, the stability during short-time speed or load transients may be assumed to be good. Nevertheless, the transient time constant T_d' on these lightweight alternators is in the order of 0.01 second, so that the high transient synchronizing torque is effective in only the initial moment of the longer transients. It may help in releasing the overrunning clutch on a slow alternator, and it should give material stiffness in opposing engine-torque pulsations, as these involve principally upwards of 30 reversals per second.

More experience with these lightweight alternators, driven from the main engines through constant-speed drives, is necessary before the importance of the various design factors can be evaluated correctly. As I have pointed out in the paper, the condition of main engine drive introduces certain difficulties which are not encountered with auxiliary engine drive. This emphasizes the importance of co-operation in the design of the constant-speed drive and governor to minimize the natural rigidity of this coupling. I have mentioned the desirability of incorporating in the drive some degree of softness or slip proportional to load. This need not involve a direct power loss if it is obtained by a change in ratio. In some of the proposed drives this may be done by taking advantage of the inherent torque reaction upon the ratio-changing mechanism.

Boice and Levoy list certain disadvantages of parallel operation of a-c power units. The alternator control and governor weights are somewhat greater, but the increase must be considered in the light of the following factors:

(*a*). Improved alternator load factor, particularly in starting the larger motors.

(*b*). Improved fault-clearing ability, because more current is available to burn faults clear or to isolate the faulty section by blowing limiters.

(*c*). Weight of additional load-transferring equipment required for strictly nonparallel operation.

It is implied further that difficulty will be experienced in dividing load between paralleled a-c units. There is no reason for expecting a degree of load division which is inferior to that obtained with the present low-voltage d-c systems, as this function of the governor does not present a particularly difficult design problem. The paralleling control system, which is being developed, merely provides a convenient facility for the flight engineer to exercise a vernier adjustment of load division, if he so wishes, without having to leave his flight station.

The wiring weight figures given by Boice and Levoy show clearly why the larger airplanes vitally need a high-voltage distribution system. The higher weight given for the 120/208-volt a-c system is the result of the power factor. Because, at a voltage of 120 volts above ground, line drop is not a major factor, wire sizes may be selected on the basis of temperature rating. For this reason each of the three a-c line wires may have less than one third the copper section of the 120-volt d-c system because of the improved heat dissipation from the smaller-sized cable. Of course, this same principle may be applied to the d-c wiring by the use of multiple circuits in parallel.

In regard to Mr. Veinott's interesting comparison of alternator weights, he included an allowance for the self-excitation power amounting to approximately two kilowatts at the five-minute rating. This figure includes the losses of the electronic regulator.

High-Voltage-Ignition-Cable Design for Aircraft

Discussion of paper 43-145 by H. H. Wermine, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 716–19.

F. O. Stebbins (General Electric Company, Schenectady, N. Y.): Mr. Wermine's paper describes the present problems met in the design and manufacture of satisfactory high-voltage aircraft ignition cable and points out that a method for simulating flight conditions by cycling cable tests at operating temperatures under vacuum is proving of great value in determining what aircraft ignition cable will and will not do. The problem of mechanical breakage of the insulation on the ignition wire and the effects of corona in causing the ignition wire to deteriorate rapidly are described. The difficulties of removing defective ignition wire from the ignition-shielding assembly are also referred to.

The failure of high-voltage aircraft ignition cable to meet the stringent requirements of aircraft applications has been recognized for some time. Since it was recognized several years ago that a considerable period of time would be required before these problems are solved, a method for building ignition-shielding assemblies for airplane engines using wire subject to mechanical and altitude difficulties, which reduces the effects of these difficulties to a minimum, was developed and is described herewith.

Figure 1 of this discussion shows an ignition-shielding assembly for an 18-cylinder aircraft engine. This assembly consists of two parts, the manifold and the 36 flexible leads. The manifold contains high-voltage aircraft ignition cable and is filled with gel, so that it is one solid mass of cable and gel without any inclusions of air. This prevents movement of the ignition cable due to vibration and protects the insulation from mechanical stresses, and, since there is no air present inside the manifold, the formation of corona inside the manifold with resultant destructive effects on insulation cannot occur. The flexible leads which run from the manifold to the

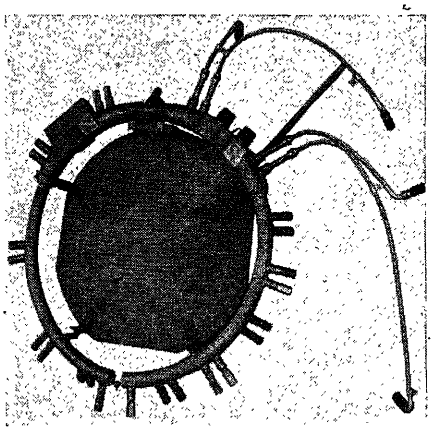


Figure 1

spark plugs are not protected from corona at high altitude and may, therefore, fail from this cause. Such a failure, however, is not difficult to repair, because these flexible leads are detachable from the manifold and can be replaced easily.

We believe that this ignition-shielding-assembly design presents a good solution to the high-voltage-ignition-cable problem on aircraft at the present time. The manifold is permanently mounted on the aircraft engine with the ignition cable protected against mechanical failure and corona, and the parts of the ignition-shielding assembly which cannot be protected suitably against these difficulties are replaceable easily in case of failure.

A. M. Ross (General Electric Company, Bridgeport, Conn.): Perhaps a further comment on the simulated engine tester mentioned in Mr. Wermine's paper may be of interest. This instrument was developed in its essential form by H. H. Race, General Electric Company research laboratory, Schenectady, N. Y., and owed its subsequent evolution to the co-operative efforts of the ignition-cable manufacturers' technical advisory committee under Mr. Wermine's able chairmanship. Introduced at a time when the causes of ignition-cable failure were obscure and the road to progress not apparent, it promptly pointed a path to the very satisfactory improvements already described to you.

In its early stages the low-pressure high-temperature high-voltage tester exposed the serious weaknesses of lacquered-braid-covered cables, demonstrating that charred-cable terminations of conventional structure had suffered even more from tracking and arc-over than from the high temperatures encountered in spark-plug wells. Lacquered-braid cables rarely withstood as much as ten hours of testing under the procedure first used, whereas some of the earliest Neoprene-sheathed cables tested exceeded 100 hours of life!

With the development of cyclic heat and pressure the simulated engine tester, never a respecter of persons, proceeded to expose faults in almost all parts of the ignition wiring system: plugs, connectors, grommets, and boots as well as cables, and it has stimulated notable improvements in the design of many of these components.

Doctor Race's equipment has yielded information so far on approximately 400 samples of various cables involving more than 13,000 sample hours of "flight" between sea level and 40,000 feet! With the latest developments of technique and equipment it is indicated that 80 per cent of the Neoprene-sheathed cables have lives longer than 100 hours of test, whereas only 20 per cent of the conventional lacquer-braid types survive this period. This estimate assumes, however, the most careful gasketing and latest improved connectors. A survey of the years' work indicates a somewhat less favorable comparison. If all types of setup and testing used are taken into account, but short-time failures of plugs or connectors are excluded, we have:

1. For 48 tests on lacquered-braid cables an average life of less than 22 hours.
2. For 64 tests on Neoprene-sheath glass-braid cables an average life of more than 131 hours.

It may be said now that the simulated engine tester is well on the way to becoming a reasonably precise means of evaluating aircraft ignition cables and should be indispensable to the development of improved cable constructions.

The success thus achieved may be credited to workers of the General Electric Company, Detroit Edison Company, Electric Autolite Company, Packard Electric Division of General Motors Corporation, and Belden Manufacturing Company who contributed to various phases of the equipment design and operating technique. The notable improvement in cables already accomplished is owed in considerable measure to many frank discussions among the cable manufacturers, the engine builders, and the various government agencies meeting in committee.

Radio-Noise Elimination in All-Metal Aircraft

Discussion and author's closure of paper 43-140 by Fred Foulon, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, pages 877–91.

C. W. Frick (General Electric Company, Schenectady, N. Y.): The excellent analysis of the problem of radio-noise elimination in aircraft presented in the paper, which pictures the chain from the influence source to the point of radio reception, brings out quite clearly the need for co-operation among the three groups: the aircraft builders, the radio manufacturers, and the electric-apparatus manufacturers. As stated in the introduction to the paper, the solution of the problem "requires that these components be considered conjointly and that each party accept his part of the total responsibility."

The electric-apparatus manufacturer's part in this solution is concerned with the radio-influence characteristics of apparatus as furnished to the aircraft builder. He does not install the wiring in the aircraft and thus has no control over such measures as might be employed therein to minimize the apparatus influence. Yet he must have some way of knowing whether his apparatus is or is not acceptable from the radio-noise standpoint. There should be a standard of acceptability based on some quantity which can be measured directly on the apparatus circuits in the factory by recognized methods. In many respects the radio-influence voltage (*RIV*), measured with the accepted radio-noise meter at the apparatus terminals, seemed to fulfill this requirement. The use of this quantity has been extensive and quite successful in connection with broadcast reception. Considerable *RIV* data have been taken on aircraft apparatus with the expectation that such information would meet the requirements. It may be noted that the author of the paper concludes that apparatus *RIV* cannot be applied, because it does not give a measure of receiver-noise output unless a direct correlation has been made. He points out that the over-all coupling between influence source and radio receiver varies widely from one installation to another. This situation has been overcome in another case by obtaining enough data on coupling to yield a representative average. Similar data on the aircraft case would be useful. Also, his objection that situations are encountered, particularly at higher frequencies, wherein noise coupling is functional with current rather than voltage does present a real problem. I have encountered such conditions in tests at ten megacycles and above.

With regard to the author's conclusion that measurements in the output of a radio receiver are the most accurate with respect to actual evaluation of the effects of radio-influence sources, I would like to point out that this reasoning is applicable only to the combination of apparatus and radio receiver as installed in aircraft. It has been my experience that measurements by the receiver-noise-output method are very useful for checking the over-all installation in an aircraft but are not applicable to the testing of individual pieces of apparatus in the factory. To get results which could be duplicated, many details pertaining to the radio set, the antenna, the circuit dimensions and spacings would have to be standardized, and the tests made in a screened room. Tests with radio sets and antennas to pick up noise produced by apparatus were tried in the early days of broadcasting but were never very successful and were abandoned when radio-noise meters became available All this experience indicates that the receiver-output method does not meet the requirements for providing a basis of acceptability for apparatus.

It seems that the best way to proceed would be to add to our present technique whatever additional factors are required to apply the present radio-noise-meter methods to the measurement of apparatus in such manner as to make results significant to aircraft operation. A voltage basis has been used successfully in other situations. For example, in the co-ordination of power and telephone systems the telephone influence factor (*TIF*) of the voltage is the basis for the acceptability of rotating machines, even though the inductive effects are known to be associated with both voltage and current.

The first step would be to obtain as many data as possible with existing methods and equipment, including measurements with radio-noise meters on apparatus and on aircraft circuits as well as radio-receiver noise-output measurements, and to study these data jointly in order to determine the next step. It is highly desirable to bring the three parties together through some joint committee or other agency, so that each party may be given an opportunity to bring out his viewpoint. This has been done in other situations. For example, the co-ordination of power systems with wire communication systems has been carried forward successfully by joint committee organizations which were set up about 20 years ago and are still functioning. Possibly some existing committee in the aircraft field could undertake this activity either directly or through a subcommittee. It is hoped that the discussion of the subject at this time will stimulate interest and lead to some definite action.

F. O. Stebbins (General Electric Company, Schenectady, N. Y.): The radio-noise problem has always been difficult to solve, wherever it has made an appearance, and the methods of travel which radio noise uses in arriving at its ultimate destinations have in some instances appeared to be uncanny and mysterious. Radio noise travels along copper wires and metallic objects in the same manner as ordinary d-c power and a-c power. However, it seems to violate Ohm's law, when it passes from one insulated wire to another wire, when it travels right through an open switch, and when, after every effort has been made to suppress it, the radio receiver is still unusable from excessive noise caused by a resonance condition and overlooked coupling circuits, and so forth.

There are two reasons why radio-noise phenomena are confusing to the ordinary layman.

1. The frequencies involved are very much higher.
2. The amount of power is very much less than is encountered in most other electric fields.

It may be said, approximately, that ordinary power frequencies are of the order of one cycle, audio frequencies are of the order of 1,000 cycles, and radio frequencies are of the order of 1,000,000 cycles. This difference in frequency for radio frequencies as compared with ordinary power frequencies of 1,000,000 to 1 causes effects which appear to be mysterious. For example, the open switch previously referred to can be considered as a condenser with a very small number of microfarads between the blade and the pole. The impedance of this condenser to ordinary power frequencies is very high but is only $^1/_{1,000,000}$ of this high value to radio frequencies. This is a very appreciable reduction in impedance, and in some instances the amount of noise in the radio receiver has been very little different, whether the switch was open or closed.

The inductive reactance of a choke coil is proportional to the frequency of the power flowing through it. At first thought it would seem that the choke coil would be the ideal suppressor of radio noise, since its impedance should be approximately 1,000,000 times as great at radio frequencies as at power frequencies. There is just one little gremlin in such a solution. Every choke coil consists of turns of wire, and between any two turns in the coil there are a minute number of microfarads. These microfarads are essentially in parallel with the inductance of the coil. Even though the inductive reactance is increased 1,000,000 to 1 with radio-frequency power, the condensive reactance in parallel with the inductive reactance decreases 1,000,000 to 1. This condensive reactance, unless the coil is of special design, provides a low-impedance path across the coil. Choke coils must, therefore, be designed to have a minimum of distributed capacitance, and one simple design that is being widely used is a single-layer coil.

Since radio-noise frequencies are about 1,000,000 times ordinary frequencies, and induced voltage is proportional to frequency, the ability of wires exposed to those carrying radio noise to pick up that noise is increased about 1,000,000 times and is independent of the insulation between the two wires. This explains the ability of radio noise apparently to pass through insulation.

The second outstanding difference between radio-noise power and ordinary power is the actually very small amount of power involved with radio noise. For example, radio-noise voltage is measured in microvolts which are millionths of a volt. If radio noise as great as a few microvolts appears on the antennas of the radio receiver, radio reception may be impossible. The volume of this radio noise gives the person hearing it the impression that the amount of power involved is large, that is, of the order of watts or perhaps kilowatts.

In conclusion, it may be stated that persons working on radio-noise problems must always keep in mind that the frequencies involved are many times higher than those encountered on ordinary circuits, and that the amount of power involved is minutely small if a solution to the problem by rational methods is to be obtained.

Fred Foulon: A careful reading of C. W. Frick's discussion of my paper will disclose the fact that he confirms rather than disputes the major contentions of the paper. The remaining issue seems to be that, while the paper attacks the problem from the standpoint of the evaluation of the installed radio equipment (which is the only one of real value to the aircraft manufacturer who must guarantee radio performance), Mr. Frick wishes to attack the problem from the electric-apparatus standpoint. In seeking a valid yardstick for the determination of the radio-noise influence of electric apparatus, he says, "To get results which could be duplicated, many details pertaining to the radio set, the antenna, the circuit dimensions and spacings (that is, coupling factors) would have to be standardized...." "... this experience indicates that the receiver-output method does not meet the requirements for providing a basis of acceptability for apparatus." He points out further that the author's reasoning, that measurements in the output of the receiver are the most accurate with respect to actual evaluation of the effects of radio-influence source, "is applicable only to the combination of apparatus and radio receiver as installed in aircraft" but is "not applicable to the testing of individual pieces of apparatus in the factory." This is only another way of stating the author's contention that a measurement made on electric apparatus is no measure of actual radio-noise output, unless all coupling factors are known. This contention extends even further. *RIV* measurements are of little value when made on aircraft wiring or terminal points. They may begin to be of value when made on the antenna circuit of the receiver in question, but then only if the receiver is calibrated in terms of input noise voltage. The present evaluation in terms of sine-wave voice-frequency input still leaves an undetermined correlation between noise-wave and sine-wave output with which to reckon.

The measurements of coupling factors and determination of values of *RIV*, he suggests, have already been made to a certain point. This work has shown definitely that the nature and complexity of these factors and their variation is such that only basic rules can be laid down for their control, and that the degree to which these rules must be followed again varies greatly with the installation. The realistic fact is that there simply has not been time enough to organize this work through co-ordinating committees, as he suggests, and to obtain the standardization necessary to achieve the required control over apparatus, wiring practices, and design of radio equipment.

He is quite correct in stating that very extensive work is yet to be done, with respect to the determination of the various coupling factors involved between radio-noise-influence sources and actual production of radio noise in a certain receiver, could well be done through the medium of joint committee organizations. He cites the co-ordination of the radio-noise work between power systems and wiring communications systems as having been carried by joint committee organizations that were set up 20 years ago. The work of the joint co-ordination committee on radio reception of Edison Electric Institute, National Electrical Manufacturer's Association, and Radio Manufacturer's Association and an analysis of contemporary writers on this subject[1] discloses the fact that an enormous amount of time has been spent on the measurements of noise-influence voltages with respect to field strength of electric apparatus and its associated wiring; but not until 1942 has a real correlation been made between these sources of radio-noise influence and their actual effects in terms of radio-noise output of receivers. The determination of these coupling factors, as given in the AIEE paper by Mr. Frick, "Measurements Pertaining to the Co-ordination of Radio Reception With Power Apparatus and Systems," is the first evidence in some time that the final link (all coupling factors combined) in this very difficult chain is about to be evaluated. It is obvious that, until the final link is evaluated, satisfactory radio performance cannot be predicted by radio-noise-influence measurements made at any point outside the receiver.

The aircraft manufacturer finds himself working from the other end of this long chain, in that he must guarantee the performance of the radio equipment. Former specification requirements which stipulated the use of much bonding and shielding were supposed to provide an automatic guarantee of noise-free radio performance but were found unusable and burdensome to the aircraft manufacturer and services alike. The test "by fire" of having to make the radio equipment work has simply proved that the approach must be from the standpoint of cutting the noise chain at the point it can be done most economically. Many hundreds of measurements of *RIV* throughout the airplane has given us the average picture for which Mr. Frick is looking. This picture has told us simply that there are certain things we can do to avoid *RIV* coupling in the first place by proper design and layout of our wiring and radio equipment, and that some unanticipated trouble items must be hunted down in the first complete installation. We have then available to us certain simple means of suppressing the leftover noise troubles. Such correction is simple enough not to hold up the production of the airplane. Separately encased filters are used as a stop-gap measure where insufficient filtering has been incorporated in the equipment affected. These filtering elements can later be incorporated integrally in the equipment affected. In airplanes not having the benefit of the all-metal shield the coupling between wiring and antenna is undoubtedly more difficult to reduce to a satisfactory point. In this case a greater consideration must be given the actual effect of the *RIV* source.

In a specific case where extreme coupling exists between the antenna and a *RIV* source (and the frequency range under consideration is very wide), it is possible that a filter of several pounds weight representing a large percentage of the weight of the *RIV* source may be required. The same source may operate with no effect of radio noise in another installation. Hence, it is obvious that if the conditions of installation can alter the situation to such a degree that any *RIV* limits for apparatus determined as acceptable must take these factors into consideration.

The obvious solution is to obtain the lowest possible *RIV* output of any given design by avoiding practices known to produce high *RIV* but to avoid expensive suppression means until proved necessary in a specific installation. Co-ordination between the equipment manufacturer with the user of the equipment hence, is indicated at this point. To attempt to utilize suppression means that will be certain to meet all requirements is simply uneconomical and probably unattainable.

In answer to Mr. Stebbins discussion with respect to my paper, it may be noted that there is only one contention which has a bearing on the material of the paper. Mr. Stebbins holds that the problem is one of keeping the radio-noise-influence power on the airplane circuits at low values. It is exactly this approach which is untenable when all of the considerations of electric-equipment design, aircraft electric-circuit layout, and receiving equipment are considered in the fabrication of the airplane. That this approach may work well from the standpoint of the industrial radio-interference interaction among industrial equipment, communication circuits, and ground-based receiving circuits is not at all to say that it will work in the airplane where the relationships of noise influence producing electric equipment, aircraft wiring, and receiving equipment are entirely different. It is held that it is simpler to cut the chain of *RIV* coupling some place than to attempt to control the manifold *RIV* sources. This point is more fully developed in the preceding rebuttal to C. W. Frick's material.

Mr. Stebbins also fails to differentiate between radio noise which may or may not result from radio-noise influence of electric apparatus. It is not correct to assume that the determination of *RIV* at one point or another establishes the fact that radio noise will result in connected receiving equipment. The statement that "radio noise should be kept at a sufficiently low level so that the signal can be received" is quite correct; but the statement that "the problem is really one of keeping the power at radio frequency on the airplane in the order of microvolts" is definitely not in accord with the author's findings based on many tests.

Reference

1. References 13, 14, 15, and 16 of the paper.

The Automatic Welding-Machine Starter and Its Relation to Maximum Utilization of Power and Facilities

Discussion and author's closure of paper 43-139 by Noel E. Porter, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 728–30.

Abe Tilles (University of California, Berkeley, Calif.): Mr. Porter effectively presents an important and timely subject. Anyone creating additional power-supply facilities in effect out of thin air is indeed the man of the hour. It would be of interest if he would compare initial expenditure for added facilities and for starters, not only in dollars but also in pounds of copper, steel, and rubber.

As I am engaged in development work at Kaiser Company Richmond yard three (sister yard to the one with which Mr. Porter is associated) on control units of the general nature discussed in his paper, I have some comments and questions falling under the headings of cost, timing, and psychological factors.

Cost

The cost figures are good as representative figures. The facilities' first cost per kilovolt-ampere appears conservative, that is, low. Under present conditions high-continuity distribution systems might well cost more, perhaps more than twice the figure cited. On the other hand, our power costs figure even lower than the figure cited. At the low rates that have been on file for many years by the efficient utility serving our yard, and at the high load factor which we are able to maintain (above 80 per cent), we are reduced to the paltry expedient of saving machine-idling power at a total incremental cost of 5.63 mils ($0.00563) per kilowatt-hour.

At current rates for labor, three minutes of an operator's time cost more than power charges for three hours of a machine's idle operation. If the operator could not make two round trips in three minutes, it would be uneconomical for him to shut off his machine, quite aside from other possible objections. If such a machine is to be stopped for less than a half shift of idleness, it must, therefore, be stopped automatically. Fortunately, very inexpensive control units can be assembled.

It is to be expected that maintenance of motor main-line contactors will be increased by increased number of stops. It is also the consensus of opinion that decreasing idle generator operation will noticeably decrease the commutator maintenance required, and that this saving will outweigh other maintenance items. What is Mr. Porter's experience in this regard?

Although his general conclusions on power saving and number of machines simultaneously in operation are clearly sound, it would be of interest if Mr. Porter could cite a direct comparison between his probability calculations and observed data, preferably on a large number of machines.

Timing

In discussing the timing, one might use the term "idle time" to refer to substantial periods of idleness, for example, periods of more than three minutes, and work time to periods when the machine is substantially in use. That is, work time is used here to include both arc time and pauses, the brief nonarc intervals normally alternating with arc time. Normally, arc times from one-fourth to two minutes in duration alternate with pauses from one-half to one minute in duration. During these pauses the welder moves to a new spot, raises his hood to inspect the weld, brush off slag, insert a new rod, and so forth. Distinct from these pauses included in the working time and taking up about half of it are the large intervals of machine idle time, when the operator takes off his hood and engages in, or waits for, other essential steps of the fabrication process, such as grinding down a surface; cutting work with a torch; lifting, measuring, and clamping additional work in place.

Study of records shows that on some work the pause, as defined previously, reaches one minute. That is, practically all the numerous nonarcing intervals are less than one minute. Practically all nonarcing intervals greater than one minute are the initial portions of much longer idle times. On other types of work the pause may be less than one-half minute. Our observation is that the length of the pause varies between operators and for various kinds of work but usually ranges from one-half to one minute. Mr. Porter's experience in this regard would be of interest to all.

It seems clear that the average delay setting desired of an automatic control unit is an interval somewhat larger than the pauses encountered. This delay should be preferably not too much larger than the pause interval, but in this regard the adjustment is not critical.

It has been our observation that an average delay setting of two to three minutes is entirely satisfactory. Some possibility of adjustment within this interval, and also beyond, is obviously desirable in a secondary way, as for the psychological satisfaction of operating departments involved. Precision in setting this delay interval, while it may be a source of some satisfaction, is in no way essential to materializing the benefits to be obtained from the control unit. Mr. Porter's conclusions in this regard would be of interest.

We found that satisfactory delay settings gave on the order of 15 stops per shift, and the machine disconnected 60 per cent of the total time. If Mr. Porter could give similar data on number of stops and per cent of time saved as a function of delay setting, that would be of great interest.

What does Mr. Porter estimate as an over-all delivery time for control units in quantity, if one starts from "scratch," ordering the component relays, and so forth?

Psychological Factors

A sensibly instantaneous start at all times is, as Mr. Porter points out, a psychological prerequisite. Has Mr. Porter encountered any classes of work on which reasonable objections have arisen to the presence of a good control unit?

In introducing units in quantity, what type of instructional program was used to avoid such difficulties as having operators "borrow" machines "because they weren't running anyway?"

Knowledge that a foreman on the upper deck can tell whether he is working or not by looking at his machine should tend to exert a slight steady pressure on the operator to keep busy. This alone might prove a most valuable item.

It is, of course, possible to go a step further in this direction and include an arc-time totalizing equipment within the control unit. Conceivably, this might apply such pressures as to arouse strong uncontrolled resentments, competition among operators for easy continuous work, and so forth. Its suitability in a yard where the total basis of operation is hour work and not piecework seems open to question.

As Mr. Porter has had experience with both of the previously mentioned degrees of incentive, his comments on this point will be of interest.

R. R. Dysart (United States Maritime Commission, Oakland, Calif.): As electrical engineer for the United States Maritime Commission on the West coast, part of my duty has been to study and analyze the benefits derived from the use of automatic welding controls. You and I have often heard the expression, "Oh, what do we care about expenses. The Government pays for it anyway." Of course that is neither patriotism nor horse-sense economy, but hundreds of people are saying it every day. But don't forget, each sum of money wasted, whether large or small, comes out of our own pockets.

Thanks to engineers and the electrical industry for the low-cost power available for public use. In some types of ships that are being built on a production basis, the cost of electric power is less than one per cent of the total cost of labor, but, nevertheless, if the one per cent represents several thousand dollars, it is to our mutual benefit to give due consideration to economies where possible. In no case should speed of shipbuilding be sacrificed for economies which would be overshadowed greatly by prolongation of the war even by a single day or hour.

Actual performance-test data obtained from a group of controls operating in connection with an area in the shipyard where detailed observations have been made presents the following benefits:

1. Conservation of critical materials, represented by a reduction in

Kilowatt demand.
Kilowatt-hours used.
Generating capacity and transmission facilities.
Maintenance.
Power-factor improvement.

It may be seen readily that each of the preceding represents real savings in dollars. The savings, as pointed out by Mr. Porter in his paper, have actually been realized; therefore, the investment in the control device contributes a saving not only in present-day critical materials but in dollars as well

2. Conservation of critical man power.

The arc-time totalizer, which contributes to increasing production efficiency, cannot be evaluated in as simple terms as the automatic start and stop feature of the control device. However, tests are beginning to show and the welder's personal response is beginning to indicate that production efficiencies are increasing materially. The entire welding department is beginning to realize how easy it is to increase its output and to retain that improvement. A relatively small increase in footage welded per man per day represents savings in dollars and critical man-hours far greater in magnitude than the direct savings outlined in the foregoing text. Therefore, it is considered that both the automatic-control feature and the arc-time totalizer are very important contributions to the industries using welding in our all-out war program.

Noel E. Porter: The author thanks Doctor Tilles and Mr. Dysart for their constructive and interesting discussions on the paper.

It is believed that Doctor Tilles's analysis of arc time and normal working pauses is correct, particularly for horizontal welding. For vertical and overhead work, however, the normal working pauses may often be more than one minute in duration. A further substantiation of Doctor Tilles's analysis was shown in test conducted for average welding conditions with various time-delay settings. At three-minute delay, the number of starts was found to be 17 per eight-hour shift. With a delay of one-half minute the number of starts increased to an average of 80. The curve of starts versus time-delay setting bends noticeably at the $1^1/_4$-minute-delay setting point, so that the number of starts increases rapidly with settings below this value. Only a slight variation was noted in the number of starts as the average arc time was varied from 20 to 30 per cent. See Figure 1 of this discussion.

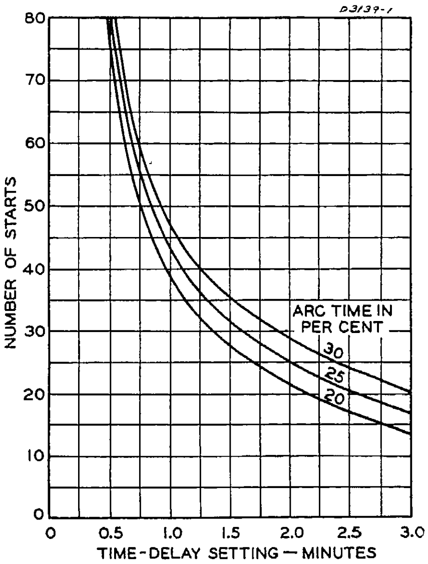


Figure 1. Number of starts versus time-delay setting during eight-hour period

Matters of impulse records, timing, wear and tear on contacts, and other specific properties of the components of various types of starters have been studied and are undergoing considerable investigation at this time. However, the very rapid increase in number of starts with reduced time-delay setting would lead one to believe that accuracy in delay setting is a prerequisite of a satisfactory unit. Preliminary results from an accelerated life test on two types of starter units reveal that wear and tear on the welding-machine line starter will exceed by far wear on the contacts of the automatic-starter relay contacts. Whether or not the reduced wear on the commutator due to lower total operating time will offset entirely the increased maintenance cost on contactors is yet to be determined fully. It is somewhat probable that the two factors will balance on a maintenance-cost basis.

H. S. Doolittle, in a recent report to the management of the Permanente Metals Corporation and to the United States Maritime Commission, gives the test results on 135 welders equipped with automatic starters. With an average arc time of 35 per cent, welding power was reduced 22 per cent. These results check favorably with the method of calculation outlined in the paper. In general, for arc times up to 30

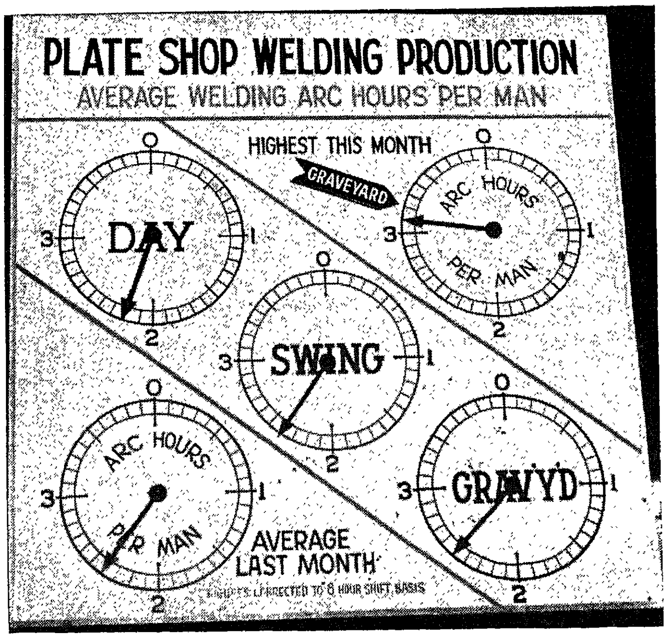


PLATE SHOP WELDING PRODUCTION
AVERAGE WELDING ARC HOURS PER MAN
HIGHEST THIS MONTH
GRAVEYARD
ARC HOURS PER MAN
DAY
SWING
GRAVYD
ARC HOURS PER MAN
AVERAGE LAST MONTH
FIGURES CORRECTED TO 8 HOUR SHIFT BASIS
0
1
2
3

Figure 2

per cent, machines with starters will run for a total period equal to twice the arc time. Thus, for 25 per cent arc time the machines are disconnected for 50 per cent of total time. Above 30 per cent arc time, starters save half or slightly more than half the normal no-load running time.

Mr. Dysart's comments on the importance of arc time to increased production are being substantiated rapidly in our experience. Incentive systems based on arc time have been used successfully for many years. In our plate shop an arc-time board in plain view gives the average arc-hours per man for the previous day for each shift, as shown in Figure 2 of this discussion. Competition is between shifts and is based on the total number of welders for each shift and corrected to an equivalent eight-hour basis for all shifts. After this control system was instituted, in one month's time average production per man increased nearly 20 per cent. Similar control boards are being installed now in other welding production areas.

The possible detrimental psychological effects resulting from the use of arc time as a basis for welding checking and production control are contingent entirely upon the manner in which it is presented and upon labor relations and local conditions. It is believed that arc-time control, when properly applied, will increase inevitably welding production per man, because, in general, it gives the fairest index to actual welding work performed.

Effect of Frequency on Let-Go Currents

(43-134)

Effect of Wave Form on Let-Go Currents

(43-135)

Discussion and author's closure of paper 43-134 by Charles F. Dalziel, Eric Ogden, and Curtis E. Abbott, and paper 43-135 by Charles F. Dalziel, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, December section, pages 745–50 and 739–44.

J. J. Smith (General Electric Company, Schenectady, N. Y.): Dalziel, Ogden, and Abbott have given us interesting data on the measurements of the magnitude of let-go currents on a large number of subjects. Their results indicate that the value let-go current is about nine milliamperes for men and six milliamperes for women. These values are based on 60 cycles voltage and cover 99.5 per cent of the cases tested.

In their conclusions, the authors make the following statement based upon the foregoing results:

> "The reasonably safe 60-cycle current for most normal healthy adult men is about nine milliamperes; the reasonably safe 60-cycle current for most normal healthy adult women is about six milliamperes."

In a paper, "The Effect of Electric Shock on the Heart" by Ferris, King, Spence, and Williams,[1] the following statement is given in conclusion 3:

> "A current just below the threshold for ventricular fibrillation is the maximum to which man safely may be subjected. Based upon numerous tests on animals of several species comparable in size with man, this maximum current is about 0.1 ampere, for a duration of one second or more and a pathway between an arm and a leg."

Obviously, the safe current referred to in the Ferris paper is not the same type of safe current referred to in the present paper. It seems, therefore, more appropriate to refer to them as limiting values of current. It may be noted also that in the present paper some of the human subjects were shocked up to 25 milliamperes, and when the current was removed they suffered no ill effects. This would indicate that there is another limiting value of current which is of interest and which is considerably higher than the values in the conclusion of the paper, namely, that which may be applied before the effect on the human system is such that artificial resuscitation is necessary. Ferris and his colleagues suggest that this value is below the threshold for ventricular fibrillation but give no data on this point.

The fourth type of limiting current might be regarded as the threshold of feeling in connection with electric shock. This is of importance in connection with the design of electrical apparatus for the home. As an illustration, I recollect a case where a woman received a shock by having one hand on an electric mixer, which she was using, and the other hand on the door of the refrigerator. Being alone at the time, she decided, wisely, that the kitchen was alive and would not enter it again until an electrician had looked over the situation. Investigation showed that the maximum current that she could have received was about one milliampere, and this was due to a capacitor which had been placed on the device to reduce radio interference. The installation of a smaller capacitance for this purpose eliminated the sensation of shock. This example, however, indicates clearly that a limiting value of current for such household appliances should be based upon the threshold of feeling rather than let-go currents or any other type of current. A joint committee of the Edison Electric Institute, National Electrical Manufacturers' Association and Radio Manufacturers' Association[2] has studied this question and recommends, so as to avoid the possibility of a shock which may be felt, a limiting value of 0.3 milliampere for the maximum current which may flow through such a capacitor to the frame in devices which normally are used ungrounded.

Thus, we have at least four types of currents which we are interested in having definite data on:

1. The threshold of feeling:

This is of interest in connection with the design of electrical apparatus for general use. The value of this has been given by EEI, NEMA, or RMA as not to exceed 0.3 milliampere.

2. The let-go currents discussed in this paper:

These are of prime importance when we are interested in situations, such as electric fences, where it is desired to shock the subject but not to such an extent that there is danger to him if he should be alone and become unable to disconnect himself from contact with the circuit.

3. The maximum current which the subject may be able to stand without becoming unconscious and thus necessitating recourse to artificial respiration:

This value is of importance in connection with the work of electricians and engineers who, in their work, are subjected necessarily to greater possibility of contact with electric circuits and, with this knowledge, may be expected to work under conditions which will safeguard them as far as possible. I have been unable to locate any definite information on this value in the literature, but Ferris and his colleagues suggest it lies below the threshold value for ventricular fibrillation. It would be interesting to have some definite data on this subject, and I hope some of the workers in this field can fill this need.

4. Finally, the value which produces ventricular fibrillation:

This, of course, is of great interest, since the experiments indicate that artificial respiration will not help in the case of the subject who has reached this stage, and that, if recovery is to be achieved in such cases some additional measures, such as countershock, must be applied. Ferris and others give 100 milliamperes as the value for this current.

It should be noted that the foregoing values I have given are those which apply to a normal adult. If the correlation which has been indicated in certain papers between the size of the subject and the amount of current is valid for all types of shock, these values would have to be reduced for the case of children. In fact, there is on record a case[3] studied by the Oregon Agricultural College, where a current of not more than nine milliamperes electrocuted a four-year-old boy.

The subject of the effects of electric shock is quite important, and I believe it will profit us materially in our studies if we subdivide it into the four divisions that I have outlined previously, each of which is important in its own connection. Those who have a problem in this respect then will have authoritative information available, and it could be used to good advantage in dealing with any other phases of this subject.

REFERENCES

1. THE EFFECT OF ELECTRIC SHOCK ON THE HEART, L. P. Ferris, B. G. King, P. W. Spence, H. B. Williams. AIEE TRANSACTIONS, volume 55, 1936, May section, pages 498–515.

2. NEMA publication 105.

3. International Association of Electrical Inspectors *News Bulletin*, November 1940.

H. B. Williams (Columbia University, New York, N. Y.): The conclusions on the effect of frequency are in line with the findings of physiologists on the stimulating effect of electric current at various frequencies on isolated living tissue, such as nerve.

In view of the fact that sudden death from contact with low-voltage circuits is most often caused by ventricular fibrillation, and that the currents which have been found by animal experimentation to be necessary to produce this condition when passed from a foreleg to a hind leg on animals with chest dimensions similar to those of man are considerably larger than the let-go currents for the same frequencies, the conclusion reached by the authors that the let-go current should be regarded as the maximum to which human beings may be exposed safely seems to be a sensible one. There seems to be no question but that certain individuals may be killed by currents insufficient to stop the movements of respiration. Currents at the let-go level are painful and hard to endure even for a short time. If they are continued long they may lead to a rise in blood pressure which throws strain on the heart. In persons with a damaged heart this may result in inability of that organ to maintain the circulation, and death may then occur.

The effects at frequencies above the usual commercial power frequencies are of particular interest in connection with the increasing use of electronic devices in which current of higher frequencies is often produced, often at comparatively high voltage and with the possibility of delivering relatively large currents. Although the finished equipment is usually well protected, there may be danger to personnel engaged in development work who have to work with energized equipment without protective structures.

At the higher frequencies large currents may pass without causing stimulation of muscle or nerve, and these may cause deep heating effects. Since the heat-sensitive mechanism is located in the skin, there is the possibility of damage to internal organs by high-frequency currents, even though no very unpleasant sensation may be apparent. The currents necessary to produce this effect probably would be of the order of an ampere or more.

Not only the general public, but also electrical workmen should be cautioned against careless contact with any power line. The impedance between points of the human body beneath the skin is of the order of 300 to 400 ohms for 60-cycle alternating current. Hence, it is evident that the only thing which protects against severe and dangerous electric shock on contact with a 120-volt power line is the high resistance of the dry uninjured skin.

Knowledge that thorough wetting of the skin lowers its impedance and produces a hazard is becoming general, but it should be emphasized that cuts, scratches, or abrasions of the skin, or broken blisters, may have an even greater effect in lowering resistance than mere wetting. Electrical workers should protect carefully such minor injuries.

Much can be done for the protection of the general public by correct design and construction of equipment, but those whose duty it is to install and maintain such equipment must be aware not only of the well-known danger of contact with circuits of high voltage, but also of the exceptional but still not uncommon, conditions which make contact with any power circuit hazardous.

With reference to "Effect of Wave Form on Let-Go Currents," where alternating current of frequency 100 cycles or less is superimposed on direct current, it undoubtedly is true that the stimulating effect depends on the crest value. In attempting to predict the effect of current pulses of very short duration we must take account of the fact known to physiologists that as the duration of a current diminishes its strength must be increased in order to produce stimulation, and as the duration becomes very small this increase must be very great, finally becoming so great that destruction of living substance occurs before it can respond. Sinusoidal currents of frequency half a megacycle and upward are used for heating living tissue without stimulation. This might lead one to suspect that single-current pulses lasting but a few microseconds might be harmless, and even their repetition might not stimulate. This is not true when the peak voltage and peak energy of such pulses are high and when they are repeated about 200 times per second. In view of the extensive use of electronic equipment generating such pulses, it is important that personnel be protected from contact. It is especially important to guard engineering personnel engaged in development of such equipment, since they have to work on exposed circuits. The dangerous character of such pulses has been determined by animal experiment. When the war is over it would seem highly desirable to investigate the effect of alternating and pulsating currents which depart very greatly from the sinusoidal form and, particularly, of pulses of approximately square-topped form of short duration, but of high peak voltage and energy.

It would be desirable also to investigate the effect of condenser discharges with various time constants. Underground power cables operated at 10,000 volts are capable of delivering a powerful shock, and even after momentary grounding a considerable afterdischarge may be delivered.

H. W. Bousman (General Electric Company, Schenectady, N. Y.): We all are indebted greatly to the authors for their persistence in measuring let-go current on enough subjects to get information which can be treated by statistical procedures. These data will be used undoubtedly as a basis for design where electric-shock hazard must be controlled. There are two points arising in the interpretation of these test results which I should like to discuss:

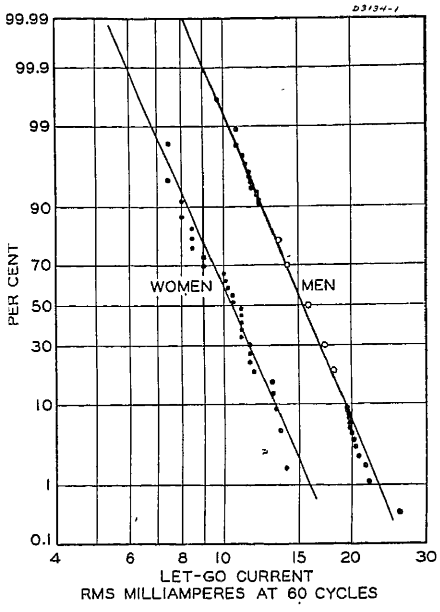


Figure 1. Per cent of adults with 60-cycle rms let-go current less than value shown

Redrawn from Figure 1 of "Effect of Frequency on Let-Go Currents"

1. The authors have based their conclusions on a value to protect 99.5 per cent. This leaves one half of one per cent unprotected. It may not be feasible to protect every person, but it seemed desirable to see how the current might vary if a different value for the percentage protected were taken. By extending Figure 1 of the paper to 99.99 per cent, we find that for this value of protection the current for men at 60 cycles should be approximately six milliamperes which is appreciably below the value of nine milliamperes given by the authors.

2. In Figure 1 of the paper the results on the subjects who withstood least current and most current, respectively, depart most from the best straight line. This suggests that some function other than linear correspondence should be used to describe the physiological response to stimulus by electric current. When the precedent is used that responses to stimuli of other sorts, such as light and sound, show approximate proportionality to the logarithm of the stimulus, it seemed worth while to replot in terms of the logarithm of the current. Figure 1 of this discussion is so plotted and shows the points, including the extremes, to lie very nearly on a straight line, thereby empiricially justifying the assumption of logarithmic response. By so replotting in Figure 1, we find the let-go current which will protect 99.5 per cent is ten milliamperes and that which will protect 99.99 per cent is eight rms milliamperes. The corresponding limits for women are seven and five milliamperes. This discussion refers to adults only. It is probable that children would experience a considerably lower limit of let-go current.

J. E. Wise (nonmember; the State of Wisconsin Industrial Commission, Madison, Wis.): The information presented by Dalziel, Ogden, and Abbott is very interesting, especially in view of the regulations with regard to electric fences which have been adopted by the Industrial Commission of Wisconsin. Our earlier study of the problem was limited almost entirely to fence controllers delivering an alternating or a pulsating current. A study of the literature with regard to hazards of electric current and consideration of the available units resulted in the rules which, with an interrupted a-c controller, limit the current to 25 milliamperes with a shock interval of not to exceed 0.1 second and an "off" period of at least 0.9 second. For a battery-operated unit of the inductive-discharge type, the maximum peak current of 500 milliamperes and a total current-time product of three milliampere seconds have been specified. For the capacitor-discharge unit we have the same general requirements and have indicated that the current between shocks shall not exceed five milliamperes. Complaints have been made that this current is so small as to make it impossible to maintain a charge on the capacitor in cases where there may be a leakage due to either vegetation or moisture on insulators.

It seems, from the material presented in this paper, that it may be possible to increase the allowable current between shocks without the danger of a person or animal being "frozen" to the fence, and by proper regulation of the charging current the discomfort may be reduced.

A further study of the paper undoubtedly will be of great interest and will aid in preparing workable regulations.

J. A. Dickinson (nonmember) and **F. B. Silsbee** (National Bureau of Standards, Washington, D. C.): The pair of papers by Professor Dalziel and his colleagues on the effect of the frequency and of the wave form of an electric current on the shock felt by a human victim forms a valuable rounding out of the work which he has been carrying on in this field for several years. It is to be hoped that, when peace permits the resumption of such researches, these studies on continuous currents will be supplemented by similar work on electric transients of short duration. There is at present a serious dearth of information on the tolerance limits and physiological effects of such transients.

With modern techniques using the cathode-ray oscillograph, it should be possible to measure such effects and to correlate them with various times in the heart cycle.

A rigorous mathematician might question the validity of concluding as a general theorem that "let-go currents were controlled by the peak of the current wave" on the basis of only four types of wave.

1. Sinusoidal.
2. Triangular.
3. Peak produced by third harmonic only.
4. Dimple produced by third harmonic only.

For instance, would a sharp peak of a few microseconds' duration superposed at the crest or at the zero of the main wave be felt? However, such freak waves seldom would be encountered.

The deductions made regarding safe or "reasonably safe" currents for children would appear to be based on very limited data. Admittedly, it is hard to get children, particularly small children, to serve as human guinea pigs. If we are to assume that a healthy child can stand the same current density through an arm or leg as can an adult and assume geometrical similarity, the comparison of a child weighing 30 pounds with an adult weighing 150 pounds would predict a factor of almost 1 to 3 between their let-go currents. Further, a child's skin is thin and generally more moist than that of an adult and would, therefore, have a lower-contact resistance. It would seem to us that a factor of 3 or 4 would be more in order than the factor of 2, which the authors have suggested between the safe currents for children and for adults.

Small children are inherently curious and often mischievous. The most dangerous age as far as traffic accidents are concerned has been found to be the four-to-five-year-old group. Such children are active, prone to run about and climb, but have yet to learn caution. Electric circuits (rural supply wires as well as electric fences) must be kept safe from children as young as this. Further, in the case of electric fences, the worst possible exposure must be anticipated. The death of a small girl in the West who was killed through contact with an electric fence while her feet were in an irrigation ditch, and the similar fatality of a small boy in Pennsylvania who lifted a charged fence wire while crossing a brook, indicate the type of exposure which must be considered in designing electric-fence controllers. It would not seem desirable from the data available to set the "safe" value for children as high as 50 per cent of that for adults.

The reader of these papers should be careful to avoid the pitfall of considering that the stimulus in the case of superposed alternating current and direct current is dependent solely on the crest values shown at A in Figure 1 of paper 43-135. Points on the upper curve of Figure 2 (43-135) do not correspond to a constant value of this crest, but to a value which is less, as the a-c component forms a greater part of the total.

An interesting point brought out by the deviation curves of paper 43-134 is that the "founding fathers of our nation" were somewhat wrong in their postulate that "all men are created equal," since some men can let go twice as much current as others. However, in the d-c trials of Figure 12 of this paper, where the criterion is mental rather than physical, all men do show up more nearly alike.

Charles F. Dalziel: The many helpful suggestions and interest given this research are appreciated greatly. Suggestions of new topics worthy of consideration will be given attention when conditions brought about by the war permit resumption of the investigation. Because of time limitations, the author is unable to comment in detail on the controversial and interesting points raised by discussers. For discussion of thresholds of feeling by the writer, refer to reference 1.

REFERENCE

1. PRACTICAL ASPECTS OF ELECTRIC SHOCK, *Electrical West*, March 1941, pages 40–5.

A New Simple Calculator of Load Flow in A-C Networks

Discussion and author's closure of paper 43-152 by Waldo E. Enns, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, December section, pages 786–90.

W. Ridgway (General Electric Company, Schenectady, N. Y.): The calculator described in this paper is designed to give direct readings of real and reactive power flow in the resistance network representation of an a-c network. The method of a-c network analysis by resistance networks has been described previously in papers by W. C. Hahn[1] and Waldo E. Enns.[2] The methods outlined in these papers differ in the values set up in the resistance networks, but both depend on the basic concept of resolving the a-c network into two resistance networks to which inphase and out-of-phase currents are applied to obtain voltage drops and real and reactive power flow. Direct readings of real and reactive power avoid the lengthy calculation necessary to obtain these quantities when this method of analysis is applied on a d-c board.

In order to obtain real and reactive power it is necessary to solve the following equations:

$$\text{Real power} = 3E_p(V_p/M + V_q/N) + 3E_q(V_q/M - V_p/N)$$

$$\text{Reactive power} = 3E_p(V_q/M - V_p/N) - 3E_q(V_p/M + V_q/N)$$

In the calculator these equations are solved by using a special wattmeter to perform the indicated operations. This requires a complicated connection and switching arrangement which must be set up for every problem.

The analysis of an a-c network by this calculator depends on the assumption of the correct values for E_p and E_q at all generator and load busses. There is only one correct value of E_p and E_q everywhere to give the correct solution for assumed values of loads and generation. The initial adjustment of the network for a previously unexplored problem would be very difficult, since all values of E_p and E_q are interdependent and may not converge rapidly.

No provision is made for direct measurement of current flow in the calculator. Under this condition short-circuit studies cannot be made.

There is no doubt that the resistance network method of a-c network analysis has merit for problems of limited scope. However, the calculator described, with its complicated connection and switching arrangement and the difficulty in obtaining correct values of E_p and E_q, would appear to offer little advantage over the previously discussed resistance network analysis of a-c networks using the d-c board, particularly since short-circuit studies are possible on the d-c board and cannot be made on this calculator.

REFERENCES

1. LOAD STUDIES ON THE D-C CALCULATING TABLE, W. C. Hahn. *General Electric Review*, part I, July 1931, page 444; part II, August 1931, page 482.

2. METHOD FOR A-C NETWORK ANALYSIS USING RESISTANCE NETWORKS, Waldo E. Enns. AIEE TRANSACTIONS, volume 61, 1942, December section, pages 875–80.

F. C. Poage (Ebasco Services, Inc., New York, N. Y.): In discussing this paper by Mr. Enns, because of its approach to the subject of power-system network analysis via the consideration of voltage drops, I wish to call attention to a much-neglected feature of all network analysis, namely, checking the results. Further, I wish to present a simple method of check and adjustment which can be applied to the results of any power-system flow analysis. This method can be applied regardless of how the analysis was made, whether by a complete a-c calculating-board study, wherein all parts of the system are set up and loaded simultaneously on one of the few large analyzers available in this country, by piece-by-piece board studies such as these described by Mr. Enns in this and his previous paper, by detailed computation, or by outright guess.

The check consists of determining how nearly the algebraic sums of the inphase and quadrature voltage drops measured in one direction around each loop of the network add up to zero. The adjustment consists of determining the separate inphase and quadrature amperes or volt-amperes which must be circulated in the respective loops to balance the loop voltage-drop components to zero. The inphase and quadrature circulating amperes or volt-amperes thus found are added algebraically to the loads in each part of the loop to determine the correct loads in the various sections of the loop.

As an example, assume the simplest of cases, two parallel lines 1 and 2 connecting busses A and B and operating at 40 kv to neutral, or approximately 66 kv. Copper sizes, conductor spacings, and route mileages are such that line 1 has 8 ohms resistance, 6 ohms reactance, and 10 ohms impedance. Line 2 on a shorter route and having larger conductor has 3 ohms resistance, 4 ohms reactance, and 5 ohms impedance. The load is 5,400 kw and 3,600 kilovars from A to B.

Table I

Inphase Drop		Quadrature Drop	
$I_{P1}R_1=15\times8$	120	$jI_{P1}X_1=15\times6\quad=$	90
$I_{P2}R_2=-30\times3=$	-90	$jI_{P2}X_2=-30\times4=$	-120
$I_{Q1}X_1=10\times6\quad=$	-60	$-jI_{Q1}R_1=-10\times8=$	-80
$I_{Q2}X_2=-20\times4=$	-80	$-jI_{Q2}R_2=20\times3\quad=$	60
Errors (volts)	10		-50

Table II

Inphase Drop		Quadrature Drop	
$I_{P1}R_1=15\times8\quad=$	120	$jI_{P1}X_1=15\times6\quad=$	90
$I_{P2}R_2=-30\times3=$	-90	$jI_{P2}X_2=-30\times4=$	-120
$I_{Q1}X_1=10\times6\quad=$	60	$-jI_{Q1}R_1=-10\times8=$	-80
$I_{Q2}X_2=-20\times4=$	-80	$-jI_{Q2}R_2=20\times3\quad=$	60
Errors (volts)	10		-50
$\Delta_1I_PR_L=3\times11=$	33	$\Delta_1I_PX_L=3\times10=$	30
	43		-20
$\Delta_2I_QX_L=-3\times10=$	-30	$-\Delta_2I_QR_L=3\times11=$	33
	13		13
$\Delta_3I_PR_L=-1.2\times11=$	13.2	$\Delta_3I_PX_L=-1.2\times10=$	-12
Final errors (volts)	-0.2 negligible		1 negligible
$\Delta I_P=1.8$ amperes			
$\Delta I_Q=-3.0$ amperes			

Table III

Inphase Drop		Quadrature Drop	
$I_{P1}R_1=16.8\times8\quad=$	134.4	$jI_{P1}X_1=\ \ 16.8\times6=$	100.8
$I_{P2}R_2=-28.2\times3=$	-84.6	$jI_{P2}X_2=-28.2\times4=$	-112.8
$I_{Q1}X_1=7.0\times6\quad=$	42.0	$-jI_{Q1}R_1=-\ 7.0\times8=$	-56.0
$I_{Q2}X_2=-23.0\times4=$	-92.0	$-jI_{Q2}R_2=\ \ 23.0\times3=$	69.0
Final errors (volts)	-0.2		1.0

Table IV

Line	Power (Kilowatts)	Reactive (Kilovars)
1	2,016	840
2	3,384	2,760
Totals *A* to *B*	5,400	3,600

The first and simplest assumption is that both components of the load divide between the two lines in the ratio of 2 to 1, that is, as the reciprocals of the impedances. Under this assumption the currents are as follows:

Inphase current { Line 1 $=I_{P1}=15$ amperes
Line 2 $=I_{P2}=30$ amperes

Quadrature current { Line 1 $=I_{Q1}=10$ amperes
Line 2 $=I_{Q2}=20$ amperes

The voltage drops around the loop can be written as shown in Table I.

It is obvious that if a current of proper magnitude and phase is circulated around the loop and is superimposed upon the assumed currents from *A* to *B*, the errors found in the voltage-drop check may be cancelled, and correct current values determined for each line.

Since, in this example, the greater error is in the quadrature drop, and the greater part of this drop is in the I_PX group, the first step is to divide the volts, +50 necessary to reduce the error in this column to zero, by the loop reactance 6 + 4 = 10 ohms, which gives a correction current of $\Delta I_P = 5$ amperes to be applied. This can be multiplied by the loop resistance $R_L=R_1+R_2=11$ ohms, which gives a correction of +55 volts to be added to the error of the inphase drop and +50 volts to be added to the quadrature-drop error.

By this correction we have wiped out the error in the quadrature drop, but we have increased the error of the inphase drop to 65 volts. Inspection of this result indicates that we obviously have overshot our inphase current correction, and, also, that we must make a similar correction of opposite sign in the quadrature current.

If we designate the loop resistance as R_L, the loop reactance as X_L, and the successive corrections by Δ_1, Δ_2, and so forth, and add them to those in Table I, the work sheet for determining these corrections looks about as shown in Table II.

The corrected values for the inphase and quadrature currents in each line are thus

In line 1 $I_P=16.8$ amperes, $I_Q=7.0$ amperes
In line 2 $I_P=28.2$ amperes, $I_Q=23.0$ amperes

When these corrected values are used, the original list of voltage drops can be repeated as indicated in Table III.

If the original assumption of division of power and reactive between the two lines is corrected, the corrected division is as indicated in Table IV.

In more intricate cases, such as that shown by Mr. Enns in his Figure 1 in which one circuit is part of two or more loops, the checkup becomes more complicated, because the various loops must be considered one after another, and the readjustments may have to be repeated. The process may be likened to that of determining the value of a mathematical series in which successive terms give smaller and smaller increments, and the practical value of the series is determined by the first two or three terms.

In cases where neither component of the voltage drop across the loop exceeds a small value, say, five per cent of line voltage, it is practical to use values of kilowatts and kilovars instead of inphase and quadrature amperes.

Glenn W. Bills (Bonneville Power Administration, Portland, Oreg.): Mr. Enns is to be congratulated for his ingenuity in designing a calculator which is not only easy to use but has the merit of being fairly accurate. Although only the actual solution of network problems with the aid of this calculator can verify it, I believe Mr. Enns' analyzer will save much time in determining voltages and load flow in a system, as compared with the usual analytical method involving a cut-and-try process. From experience on an a-c network analyzer I know that board operators can become very proficient in estimating terminal conditions on a-c networks, and the same thing should apply to this simple calculator. A very nice feature of the calculator is that, once the complete connection diagram is set up, only switches must be manipulated to obtain readings, even though only a few resistors are necessary.

Waldo E. Enns: The author expresses his appreciation to W. Ridgway, F. C. Poage, and Glenn W. Bills for their fine discussions. Mr. Ridgway refers to the paper by W. C. Hahn which also outlines a method by which load-flow problems may be solved with resistance networks using the usual form of d-c board. However, it should be noted that the method employed by Mr. Hahn was very different in concept from that employed by this new calculator.

Reference is made by Mr. Ridgway to the complicated connections and switching arrangement required for this new calculator. This may appear to be the case when one looks at the complete diagram; however, it should be noted that the plugging or connection diagram for this type of calculator is simple, so that the actual setup of a network would be an easy matter for the operator. As may be noted in the paper, the speed with which an operator can converge to the correct values of E_p and E_q to obtain the desired loading condition on a network under study cannot be known until the calculator is built and the best technique developed.

The discussion by Mr. Poage is of interest and demonstrates a technique of computing load flow in loop circuits which may be used to good advantage when a network is not too complex.

The discussion by Mr. Bills indicates that he believes that this simple calculator may prove capable of saving much time as compared with the usual analytical methods, and the writer believes that he is correct in this conclusion.

Lightning Surges Transferred From One Circuit to Another Through Transformers

Discussion and author's closure of paper 43-133 by P. L. Bellaschi, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, December section, pages 731–8.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The principal value of the paper is that once more the electrical profession is made aware of the fact that transformers are not a barrier to lightning surges passing from the high-voltage lines to the low-voltage circuits.

The principal simplification of equations given in the paper appears to be in the omission of the surge impedance of the primary lines. As shown in Figure 5 of reference 1 of the paper, this is permissible provided the transformer ratio is of the order of 4:1 or higher. For lower ratios the calculation will give values which are too high. In a previous paper[1] I have given three simple equations with two charts which allow for computing transferred maximum voltages as well as rate of voltage rise on the secondary side for different fronts and tails of the applied wave. I wish to make a correction at this time. Equation 3a of that paper should read

$$\frac{\alpha^2}{\omega_0{}^2}=\frac{\frac{3}{4}\left(\frac{T_2}{3}+C\,\frac{Z_1/r^2Z_2}{Z_1/r^2+Z_2}\right)}{T_2CZ_2}$$

In my paper it is clearly pointed out that, so far as the electromagnetic component is concerned, only the normally known values of per cent reactance, frequency, kilovolts, and kilovolt-amperes are required, besides an approximate knowledge of the primary and secondary surge impedances.

I believe it is also worth while to call attention to the work of L. V. Bewley[2] with respect to this topic. While his paper is of a much more theoretical nature, it is well to include it in a bibliography for those who wish to study the subject more intimately.

It would seem that Mr. Bellaschi considers a rotating machine winding as an inductance, so far as the transfer phenomena are concerned. It seems that most investigators of transient behavior of generator windings are agreed that these windings act primarily as a surge impedance.[3] Since surge travel is relatively slow in machine windings (about 100 feet per microsecond), the surge-impedance character should persist in most machines during the length of time represented by a lightning wave.

Table I

	Crest Voltage		
L_L	Calculated in Paper	Calculated Using C, H-L	Experiment (First Crest)
0.000354	1.2	1.08	1.1
894	2.4	2.07	2.2
1,727	3.4	2.92	3.08
4,400	4.6	3.98	4.1
∞	5.8	5.2	5.2 from Table II

The calculated and experimental test values of Table IV of the paper would check much more closely if the capacitance between high-voltage and low-voltage winding were taken into account, and the first crests of the oscillation were used in all cases (see Table I of this discussion).

Although the electrostatic component has been mentioned in Mr. Bellaschi's and most other papers on the subject, one phase of it has not been given sufficient attention. Under certain conditions of transformer operation, the low-voltage winding and the connected apparatus are not connected to ground through resistance. If the capacitance of the low-voltage network is low, then the electrostatic transfer of voltage from high-voltage winding to low-voltage winding becomes the predominant factor. The capacitance relation cannot be determined customarily from the usual known transformer specifications. In most cases the electrostatic voltage thus transmitted may exceed by a large amount the electromagnetic component, and the voltage induced in the low-voltage winding may exceed considerably that calculated by using the ratio of the insulation test levels, particularly when the turn ratio of the transformer is high. In all such cases particular attention should be given to the insulation level chosen for the low-voltage winding and its connected apparatus. Special protection in the form of grounding resistors, arresters, or capacitors may be required in such cases.

References

1. Lightning Protection of Generator-Transformer Units, J. H. Hagenguth. *Electrical World*, February 1, 1936.

2. Transient Oscillations of Mutually Coupled Windings, L. V. Bewley, AIEE Transactions, volume 51, 1932, pages 299–308.

3. References 7, 9, and 10 of the paper.

P. L. Bellaschi: The simplifications to which Mr. Hagenguth refers are recognized in the paper but certainly can stand further stressing. For instance, a surge transferred from the low-voltage side to the high-voltage side of a transformer is a possibility, and, in such case, it is apparent from the paper and the appendix that the more general solution is required. The question of high induced electrostatic effects does, of course, arise if the terminals are floating, as shown in Figure 12 of the paper, or, for instance, if simultaneous surges are applied to the two terminals, as illustrated in Figure 13 of the paper. Whether rotating machines can be considered as an inductance after a certain length of front is attained, theoretically may still be a question of discussion which hardly can be disposed here.

I heartily agree with Mr. Hagenguth in emphasizing the potential hazard seemingly nonexposed circuit can be subject to as the result of transferred surges. This condition has been recognized by the AIEE lightning arrester subcommittee in its recent report.[1]

Reference

1. Report on Application of Lightning Protective Devices in Wartime, AIEE lightning arrester subcommittee. AIEE Transactions, volume 62, 1943, September section, pages 586–8.

The Effects of Mutual Induction Between Parallel Transmission Lines on Current Flow to Ground Faults

Discussion and authors' closure of paper 43-151 by John I. Holbeck and Martin J. Lantz, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 712–15.

Waldo E. Enns (Portland General Electric Company, Portland, Oreg.): This paper is a valuable contribution, because it proves by means of actual examples that the mutual effect between parallel transmission circuits cannot be neglected in many instances without getting into difficulty with incorrect relay settings.

As is indicated in the paper, it is possible to use a d-c board for short-circuit studies involving mutual induction, when the parallel circuits are operating at the same voltage. However, when parallel lines are operated at different voltages, it is necessary to use the a-c board for the setup or to make an analytical study of considerable complexity.

Those of us who do not have ready access to an a-c board for this type of study should bear in mind that, when the mutual effects are neglected on the d-c board, there may be sufficient error in the distribution of the current to result in relay settings which will give false tripping. When this is known, if faulty relay action does occur at points where the mutual effects have been neglected, it would appear advisable that a study should be made on an a-c board to determine the correct current distribution, so that more nearly correct relay settings may be determined.

John I. Holbeck and Martin J. Lantz: With reference to the closing remarks in Mr. Enns' discussion of our paper, we wish to point out that the present d-c calculating board of the Portland General Electric Company could be changed to a-c operation provided suitable meters are obtained. In this connection, a modified d-c board converted to a-c operation has been developed by the Bonneville Power Administration and is described in a paper entitled, "Bonneville Power Administration's Short-Circuit Calculating Board," by Martin J. Lantz and Orin A. Demuth. This paper was presented before the Portland chapter of the AIEE, in the technical papers competition in May 1943.

Whenever one of two parallel lines (same voltage) are tapped, it is necessary to use an a-c network analyzer to determine the current values for a single phase-to-ground fault on the tapped line after one end of the line has opened by relay. As mentioned in our paper, an a-c network analyzer must also be used to determine the magnitudes of the ground currents for those cases in which two or more lines of different voltages occupy the same right of way.

Application of 720-Cycle Carrier to Power-Distribution Circuits

Discussion and author's closure of paper 43-141 by J. L. Woodworth, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, pages 903–15.

R. J. Schonborn (Bureau of Power and Light, City of Los Angeles, Calif.): Mr. Woodworth's paper describes very ably and completely the theory of the application of 720-cycle carrier to 60-cycle power-distribution circuits under both the shunt- and series-type of coupling.

The Bureau of Power and Light of the City of Los Angeles has in operation three installations of 720-cycle carrier-current-control equipment, two of the shunt-coupled type and one of the series-coupled type.

The three distributing stations in which the transmitters are installed are supplied with energy at 34,500 volts, 60 cycles, which is transformed to 4,800 volts three-phase, three-wire, for distribution over underground and overhead radially connected feeders.

The two shunt-coupled carrier transmitters were placed in operation in the summer of 1934 and are installed one in each of two distributing stations located in the Hollywood district.

One of these distributing stations has an installed transformer capacity of 35,000 kva, 23 feeders, and a connected load of 1,315 kw of constant-current street-lighting transformers; the other station has an installed transformer capacity of 22,500 kva, ten feeders, and a connected load of 926 kw of street-lighting transformers.

Each shunt-coupled carrier transmitter derives its energy from a motor generator set consisting of a 50-kva 720/480-cycle three-phase alternator driven by a 75-horsepower two-speed induction motor. The 720-cycle frequency is used remotely to control ornamental street-lighting circuits, the 480-cycle frequency being reserved for possible future control of water-heater load.

The bureau's experience with the shunt-coupled installations during their nine years of operation has been very satisfactory. Initially, there were a few false operations of field controllers due to interference between stations, while both were transmitting simultaneously. This trouble has been corrected by operating on a schedule with only one station transmitting at any one time.

The motor generator sets as purchased were connected by *V* belts in order to facilitate the future adjustment of machine speeds resulting from changing the then existing 50-cycle system frequency to the present 60-cycle value. After the frequency change some faulty operations were obtained which were traced to low carrier frequency due to belt slippage occasioned by imposing the carrier on the 60-cycle system at time of peak load, thereby heavily loading the alternator. This trouble was eliminated by direct coupling of the motor to the generator, which became feasible only after the system was converted from 50 to 60 cycles.

The series-coupled installation was purchased during the present emergency and recently placed in operation primarily to provide a means of blacking out quickly a number of ornamental street-lighting circuits in the event of an air-raid alarm. Previously, each of these circuits was controlled by a time clock, and, in case of an air raid alarm, the individual switch at every street-lighting transformer had to be opened manually in the field by a volunteer-citizen worker living nearby.

The series-coupled transmitter installation is located in a station having 30,000 kva of transformer capacity, 13 feeders, and a connected load of 1,167 kw of constant-current series street-lighting transformers.

Carrier energy at 720 cycles is generated by a self-driven self-excited single-unit induction-frequency converter. This single frequency is used not only to control remotely ornamental street-lighting circuits for regular and black-out operation but also for off-peak water-heater control, so that the station and system peak loads will not be increased by the water-heater load.

The operation to be performed is selected through coded impulses in the case of street-lighting control and through a single impulse of two different lengths for water-heater on-and-off control. Control may be exercised over feeders singly or in groups of three simultaneously.

The minimum 720-cycle operating voltage required at the field controllers varies from 1.25 to 1.5 volts. Tests have been made at eight field locations selected to represent a full range of distances from the transmitter to determine the value of the 720-cycle voltage actually impressed at the controller terminals under single-feeder and three-feeder group operations. The voltage actually obtained varied from a low value of 3.7 volts to a high value of 5.1 volts, depending upon the distance from the station and the 60-cycle load on the station. These values are well within the operating values of the field controllers. The corresponding voltages under single-feeder and three-feeder operation at each of the eight locations differed by a maximum of 0.4 volt.

The 720-cycle voltage impressed at the transmitting station during a single-feeder operation varies from approximately 4.75 volts at one-sixth feeder load to approximately 5.4 volts at full load with somewhat lower values for three-feeder-group operation.

Extensive tests were taken under various operating conditions to determine the value of interference or stray voltages on feeders adjacent to those on which carrier current was being impressed. The interference voltages at the station bus varied from approximately 0.1 volt at one-sixth feeder load to approximately 0.65 volt at full load. These values are too low to cause faulty operation of field controllers.

After the values of various capacitances were adjusted to obtain correctly tuned circuits, the performance of the station equipment proved to be satisfactory. The field controllers have been installed just recently and are now in the course of adjustment. As the coded impulses received in the field are of more than sufficient strength to operate the tuned circuits of the controllers, it is believed that after mechanical adjustments are completed the over-all operation will be satisfactory.

Compared with the shunt system, the series system has greater flexibility and selectivity in that an operation may be transmitted over a single feeder or a group of three feeders, whereas in the shunt system all operations are transmitted over all feeders connected to the bus to which the transmitter is coupled.

J. P. McKearin and **F. R. Longley** (Western Massachusetts Electric Company, Springfield, Mass.): The Springfield division of the Western Massachusetts Electric Company has for a number of years used 720-cycle carrier control on its street lamps. A single three-phase transmitter connected by shunt coupling to the 13.8-kv bus at a station near the center of load delivers the control signal over a network of 13.8-kv underground transmission cables to eight stations and substations and to a low voltage network. At these points it is stepped down to the distribution lines in the city of Springfield, Mass., and four adjoining towns. The territory thus covered has a population of 188,000 and a combined area of 113 square miles. A spare and duplicate transmitter is maintained at another location in a substation also near the center of load.

The present control system was installed in 1935, and, therefore, we have had eight years' experience in its operation. Prior to that time, beginning in January 1931, we had used 720-cycle control in one substation area.

The transmitter operates the switches of about 5,300 lamps out of a total of 6,600 in the territory now under carrier control. Of the remaining 1,300 lamps 750 are controlled by pilot wire, which was in use before the time of the carrier system, and 550 are on series circuits connected to constant-current transformers in substations.

The voltage level of the carrier signal is sustained well by the 720-cycle capacitance of the 13.8-kv cables, and it passes over them with almost no voltage drop, even at the most distant point. However, the voltage drop in passing through the step-down transformers to the distribution busses at the stations and substations may in some cases be as high as 50 per cent. This means that the voltage delivered by the transmitter to its 13.8-kv bus (on the 115-volt base) should be about twice that required of transmitters connected directly to primary distribution busses. We found that the minimum signal level at the distribution busses should be about four volts. This agrees with Mr. Woodworth's recommendation. At the 13.8-kv busses we need about eight to ten volts.

The large voltage drop through the station and substation transformers can be caused by double transformation and by heavy 720-cycle loads, such as 60-cycle generators, at the intermediate bus. At some stations the 13.8-kv potential is first stepped down to 5.5-kv busses to which generators and heavy industrial loads are connected and then down through another set of transformers to the primary distribution bus. At 720 cycles, the leakage reactance of transformers normally would be 12 times the 60-cycle reactance, but we have found that in some cases the 720-cycle reactance between busses is from 15 to 20 times the transformer 60-cycle value. At substations where the transformation is in one step only there is much less voltage drop between the busses, and if all were of that type we could use a lower-voltage level at the transmitter. On the other hand, if the 13.8-kv interstation transmission system should be open-wire overhead lines instead of lead-covered cables, we would need a higher transmitter voltage to allow for carrier-frequency voltage drop in these lines.

Cases of 720-cycle resonance are likely to be found at locations where capacitors are used for 60-cycle power-factor correction. This causes no damage to the capacitors, but it blows capacitor fuses, trips capacitor switches, and does strange things to the 720-cycle voltage levels in the vicinity. It is not surprising that such cases of resonance exist when it is remembered that at 720 cycles this equipment is brought 144 times closer to resonance than at 60 cycles. The reactance of the capacitor is reduced to $^1/_{12}$ the 60-cycle value, while the associated inductive reactance is increased 12 times. The normal 60-cycle reactance of a capacitor used for power-factor improvement, and the normal 60-cycle inductive reactance of its associated equipment, such as the step-down transformer to which the capacitor is connected, are such that, when the frequency is increased, the capacitive and inductive reactances approach equality and consequent resonance.

A study was made of the probability of resonance of capacitor installations at various industrial plants, and it was found that in many cases the constants of the circuit are such that at 720 cycles resonance or near resonance will exist.

The capacitor is located generally on the secondary side of the transformer bank supplying the customer's load, and the 720-cycle reactance of the capacitor often will be of the same order of magnitude as that of its transformer bank. When the plant is taking load, the load equipment in parallel with the capacitor may prevent the occurrence of resonance but will not prevent it when the plant is loaded lightly. At still other locations a resonant condition will exist when the plant load is at some point between its minimum and maximum demands. Several interesting examples of these resonant conditions are as follows:

A plant that manufactures machine tools installed a capacitor to reduce its kilovolt-ampere demand. They were troubled by persistent blowing of the capacitor fuses. A curve-drawing ammeter was cut into the capacitor circuit, and its chart showed that the times of fuse operations coincided with the times when the street lights were turned on or off. A test with the fuses replaced by solid connections showed that the 720-cycle current taken by this capacitor was 305 per cent of its 60-cycle current rating. The total capacitor current was 321 per cent of its 60-cycle rating. Calculations showed a distinct resonant condition at the carrier frequency.

Another industrial plant with a demand of about 2,000 kva had a relatively small capacitor, rated 180 kva, connected to the plant bus. The load was mainly induction motors. The plant took its power from a 5.5-kv feeder. The station to which this feeder was connected is at the farthest point on the system from the 720-cycle transmitter. At this station there were two transformations to the distribution bus:

1. 13.8-kv three-phase to 5.5-kv two-phase.
2. 5.5-kv two-phase to 2.4-kv two-phase.

Because of the distance of the station from the carrier transmitter and the double transformation, it was expected that the carrier voltage on the station's distribution bus would be low, especially at times of power-system peak load when voltage drops, both 60-cycle and 720-cycle, are at their maximum. Contrary to expectations, tests showed that at such times the 720-cycle voltage at the station's distribution bus was quite high. It averaged about eight volts. When the 60-cycle power-system load is at its minimum, voltage drops are of course at their minimum, and it was, therefore, expected that during these periods the 720-cycle voltage at the distribution bus would still be high, but when measured on a Sunday afternoon it was found to be much lower than the minimum acceptable limit. It averaged 3.4 volts when first checked. Like all the low-frequency carrier voltages, it varied some from time to time, depending upon the power-system load conditions. It also is usual to find some phase unbalance of the signal voltages. On the low phase the lowest signal strength measured on a subsequent occasion was found to be 2.1 volts.

In a search for the cause of these reversals in the normal 720-cycle voltage drops, calculations were made of the 720-cycle impedances of the apparatus in the station and of the lines and customers' apparatus in the territory which the station served. It was found that the 720-cycle reactance of the 180-kva capacitor in the previously mentioned plant was equal almost exactly to the sums of the 720-cycle reactances of the customer's step-down transformer bank, and the 5.5 kv feeder to which the transformer bank was connected. Further study showed that the capacitor was also in 720-cycle resonance with the induction motor load with which it was paralleled.

These data indicated clearly what was happening. During weekdays when the customer's plant was in operation the capacitor and induction motors in parallel with it formed a 720-cycle wave trap which blocked the flow of 720-cycle current through the power station to the customer's load. This was the largest part of the 60-cycle load on the 5.5-kv station bus, and, consequently, the flow of 720-cycle current through the station transformers stepping down from the 13.8-kv bus to the 5.5-kv bus was small. The 720-cycle voltage drop through these transformers was, therefore, small, leaving a high signal voltage on the 5.5-kv bus. The 720-cycle voltage drop through the 5.5-kv to 2.4-kv transformers was small at all times. Thus, the signal voltage at the 2.4-kv distribution bus was high at times of high 60-cycle power load.

During week ends, the customer's plant was shut down, but the capacitor was left in service. With the reactance of the induction motors removed the capacitor was in 720-cycle series resonance with the customer's step-down transformer bank and the 5.5-kv feeder. This combination formed a heavy 720-cycle load on the 5.5-kv station bus and drew a heavy 720-cycle current through the 13.8-kv–5.5-kv transformers. The resulting large 720-cycle voltage drop through this transformer bank left a very low signal voltage on the 5.5-kv and 2.4-kv busses, thus, the low signal voltage on the distribution bus at times of low 60-cycle power loads was accounted for.

Verification of this cause of low signal voltage was simple. On another Sunday afternoon the distribution-bus carrier voltage was measured and found to be about 3.1 volts. The customer was requested then to open his capacitor switch, and, upon doing so, the signal strength on the distribution bus immediately jumped to 6.4 volts. That is not so high as the weekday voltage previously mentioned. The difference was due mostly to the transmitter characteristic. The transmitter delivers a signal voltage about 10 to 20 per cent low on week ends.

A third and last example concerns a 720-kva capacitor on a substation bus. The capacitor switch was being tripped during the application of carrier voltages to the system. Its carrier-frequency current was about equal to its 60-cycle current, causing a 40 per cent increase in its total rms current. This trouble was corrected easily by increasing the trip-current relay setting.

When the possibility of 720-cycle resonance at capacitor installations was recognized we determined the characteristics of a small reactor which, when connected in series with a capacitor, will prevent the occurrence of the carrier-frequency resonance. We also recommended to the manufacturers that capacitors used in this territory be so equipped. It was recommended that the reactor have a 60-cycle reactance of 5.5 per cent (on the base of the capacitor 60-cycle reactance as 100 per cent) with taps at 3.5 per cent and 1.5 per cent and be designed liberally in winding and core to allow for some flow of 60-cycle harmonic currents.

The reactor taps are intended to permit adjustment, so that when the customer's installation is made nonresonant to 720-cycle frequencies it will not at the same time be made resonant to any of the normal 60-cycle harmonics. When the proper reactor tap is used, the total inductive reactance in series with the capacitor will lie between the limits of 5.5 per cent and 7.5 per cent. This is an optimum position between third and fifth harmonic resonance and well out of range of resonance at other harmonics. It is not practical to attempt to suppress completely the last vestige of these normal power-circuit harmonic currents. They will show up in varying amounts in almost any capacitor, whether or not it is equipped with a series reactor, but the reactor as recommended will limit them to harmless amounts

Smaller series inductances could be used to suppress the 720-cycle currents, but they are likely to set up resonance at other harmonics, especially over the week ends when harmonics of the power-circuit voltage are usually at their maximum. For example, a 4 per cent inductive reactance would be perfect for the fifth harmonic resonance and could cause destructive overheating of a series reactor.

The use of the series reactors, as has been recommended, has corrected all 720-cycle resonance conditions at customers' capacitor installations. The reactor is small in size, it adds only a slight amount to the cost of the capacitor installation, and this is partly offset by an increase of about five per cent in the capacitor kilovolt-amperes.

At two-station distribution busses the 720-cycle voltage was low and was raised to satisfactory operating levels by the installation of capacitors on those busses. One was the example described here, where low voltage was caused by resonance of a customer's capacitor. The capacitor installed in this station was adjusted to increase the signal level about 100 per cent on the low phase and 75 per cent on the higher phase.

When the power system to which the low-frequency carrier control is applied is interconnected with other systems, a considerable amount of the carrier current may escape by way of the interconnections. The transmitter should be large enough so that its output can include these extra losses, or wave traps should be installed at the interconnections. The decision is a matter of cost. On the other hand, it may be desirable in some cases to let the energy escape and thus increase the territory covered by carrier control, either for present or future use. In that event one should of course be sure that the signal voltages that appear in the outside territory are high enough to insure dependable operation of street-lamp controllers.

Extensive changes in the electric characteristics of the power system on either side of the interconnections, such as a large increase in the 60-cycle operating voltage or changes in transformer capacity at the interconnections, also can effect material changes in the flow of carrier current through the points of interconnection.

The power system of the Springfield division is interconnected at two points through transformers to a high-voltage transmission system. Within eight circuit-miles of the interconnections there are five stations and substations connected to the high-voltage lines. One of these serves a distribution system to which carrier-controlled street lamps are connected. Its carrier signal is supplied by the Springfield transmitter by way of the interconnections and high-voltage lines.

In 1942 the operating voltage of most of the Western Massachusetts Electric Company's transmission lines was raised from 66 kv to 115 kv, including the lines at the two points of interconnection. After this change had been made it was found that the load on the 720-cycle transmitter had increased from about 300 kva to 600 kva.

Some of our experiences with low-frequency carrier have had their humorous aspects. In 1935, when the present carrier control was installed, extensive tests were made throughout the power system to determine the signal strength at points of utilization. Many signals of about eight seconds' duration were delivered by the transmitter at intervals of about 5 to 15 minutes for several hours at a time. In substation transformers and voltage regulators the 720-cycle frequency superimposes on the 60-cycle hum, an audible high-pitched whine, not loud, but enough to attract attention. It is strongest in the feeder regulators. The signals penetrated to stations and substations connected to the high-voltage lines outside the territory of the Springfield division and produced the typical 720-cycle whine in their equipment. The operators in these stations were not aware of the carrier installation in Springfield and, hearing the strange noise, feared that some of their apparatus was developing incipient trouble and might be at the point of blowing up. Since the noise came from many directions and lasted for only a few seconds at a time, they could not tell which piece of equipment was involved, whether oil switch, transformer, or regulator. In one substation a bus capacitor also tripped off each time the signal noise was heard.

In short order the maintenance crew in the outside territory was flooded by calls for help and spent some hours in a frantic hunt to locate what was suspected of being a defective piece of equipment causing high-frequency voltage surges by intermittent internal arcing. Eventually, they checked with the Springfield division and were relieved greatly, but were equally disgusted, to learn the cause of the noise.

Some of the engineering problems of this carrier installation appeared difficult when first encountered, but this happened mainly because they were new and without precedent, and seemed elusive. They now are better known and are handled more readily. The control system has provided valuable advantages. It has released large amounts of sorely needed underground duct space in densely loaded areas by allowing us to change the street lamps from the series to the multiple type; it has reduced street-lamp outages some 80 per cent; and now that black-outs are on us, we have perhaps the most convenient of all controls for black-out switching of street illumination. If in the future we should need carrier control of other devices, such as water heaters, sign lights, midnight street lamps, and remote operation of power switches, it can be obtained from the same transmitter at the same frequency by use of coded signals.

John Bankus (Portland General Electric Company, Portland, Oreg.): Mr. Woodworth's paper is a very flaluable and timely contribution to electric distribution and should do much to promote the installation of off-peak utilization equipment.

The type of control which Mr. Woodworth has outlined is very flexible in its application. As was pointed out in the paper, the 720-cycle control voltage can be impressed on only a portion of one primary feeder, on one entire primary feeder, on all primary feeders from one substation, on a number of substations, or on an entire system. The capacity of the transmitter will depend on the extent of the system which is to be controlled. Local conditions will determine whether it is economical to install one large transmitter or several smaller units.

In this connection it may be of interest to see what transmitter capacity may be required for extending the 720-cycle control over an entire system which covers an area of 2,500 square miles and has a peak of 200,000 kw.

Mr. Woodworth made such a determination by setting up the Portland General Electric Company's system on the a-c network analyzer at Schenectady. The results of this study are summarized in the succeeding material. The Portland General Electric Company prepared a complete 720-cycle impedance diagram which covered its 57-kv and 11-kv network and primary-feeder system, its interconnections both with the Bonneville Power Administration at St. Johns and Salem, the Northwestern Electric Company at station *L*, and Knott substation. The load on the 11-kv interconnected network amounts to approximately 125,000 kw. The primary feeders in the urban areas are 2,400 volt and 4,150 volt. In rural areas many feeders are 7,200 volt. Four cases considered on the a-c network analyzer are described in the following:

Case 1. 720-Cycle Transmitter Coupled to 57 Kv at Knott Substation

The transmitter input (based on 11 kv) was 900 volts, 310 amperes, 30 per cent power factor lagging, 148 kw, 465 kilovars. The coverage was between 40 and 50 per cent of the total system load. This study showed such poor voltage on the interconnected 11-kv network that it definitely indicated the impracticability of providing control over the entire system by coupling to the 57-kv system only. It also indicated that traps would be required at Salem in order to reach locations at that end of the 57-kv system.

Case 2. 720-Cycle Transmitter Coupled to 11 Kv at Knott Substation

The transmitter input to the 11-kv network was 900 volts, 365 amperes, 30 per cent power factor lagging, 175 kw, and 550 kilovars. Sufficient voltage was obtained on the closer in four-kilovolt busses which are supplied by 11-kv network in Portland. At some points beyond these stations partial coverage was indicated. At more remote points the voltage was too low to be of any value. Indications were that the low reactance of 77,550-kva generators at station *L* were absorbing a great deal of reactive energy and thus reducing the voltage on the 11-kv network.

Case 3. 720-Cycle Transmitter Coupled to 11 Kv at Knott Substation with Station-*L* Generators Corrected by Capacitors

The transmitter input to the 11-kv system was 900 volts, 350 amperes, 36 per cent power factor lagging, 200 kw, and 500 kilovars. With this arrangement the coverage was better than for case 2 and was effective over 60 to 75 per cent of the total load.

Case 4. 720-Cycle Transmitter Coupled to 11 Kv and 57 Kv at Knott Substation with Station-*L* Generators Corrected by Capacitors and Traps in 57 115-Kv Transformer Banks at St. Johns and Salem

The total transmitter input (on the 11-kv base) into both the 11- and 57-kv systems was 800 volts, 460 amperes, 75 per cent power factor lagging, 400 kw, and 603 kilovars. Of this, approximately 250 kw was delivered to the 57-kv system and the remainder to the 11-kv. Adequate voltage for carrier control was obtained at all points on the 11-kv network. Also, good voltage was obtained at all points on the 57-kv network. This arrangement may be considered, therefore, to be a 100 per cent solution.

J. L. Woodworth: The discussion by Mr. Schonborn is particularly valuable, since it compares operating results obtained with both shunt- and series-transmitter coupling with 4,800-volt distribution substations on an interconnected system. The description of the initial interference obtained between the two transmitters using shunt coupling confirms the conclusions reached in the paper and emphasizes that 720-cycle traps are required to isolate each substation area if shunt coupling is applied at very many substations on an interconnected system.

The experience with the later series-coupled installation confirms the conclusions reached in the paper, that, by proper application, the interference voltage between adjacent feeders can be reduced to acceptable values at each substation, and, when this is done, the interference between transmitters at two or more substations is practically negligible.

The series-coupled installation had to meet two somewhat conflicting requirements. Control of water heaters was required by individual feeders to obtain maximum flexibility of off-peak load control, and high-speed operation was required for street-light control. The required flexibility was obtained when provision was made for controlling the feeders, either singly or in groups of three simultaneously.

The discussion by Mr. Bankus describes very appropriately the approach necessary when the chief value of carrier control is to permit an operator to obtain high-speed control and signalling simultaneously at many points on a large complex distribution system having extensive networks and interconnections. On applications of this type 720-cycle traps are required usually in the interconnections to other power systems to prevent potential interference with other 720-cycle control installations on the interconnected systems. This approach has been favored often when potential air raids chiefly determine the control requirements, or when the chief emphasis is placed on street-light control. For off-peak load control over-all economic studies usually favor control by individual substations or areas, even though 720-cycle traps may then be required to isolate areas if the system includes extensive primary or secondary networks.

The discussion by Mr. McKearin and Mr. Longley presents valuable data covering the operating experience with a central transmitter using shunt coupling to impress the 720-cycle voltage over an extensive network comprised chiefly of underground cable. The power system is considerably more complex than any of the systems studied in the paper, and the discussion should be extremely helpful to anyone planning the application of carrier control to systems of similar complexity.

The method described in which capacitors were installed at the substation busses to improve the 720-cycle voltage at certain low-voltage points should also prove valuable on other installations. Capacitors can be applied frequently to improve the 720-cycle voltage and thus reduce greatly the transmitter power required.

The discussion regarding the 720-cycle operating experience with power-factor-correction capacitors in Springfield appears rather formidable, until it is realized that it covers a five-year operating period and a large number of capacitor installations. The use of a series reactor proved to be a simple and effective way to eliminate trouble due to series resonance between the capacitor and the transformer reactance. The practice being followed of recommending a 5.5 per cent series reactor for every capacitor installed in this territory probably was decided on, because the capacitors are the property of industrial customers, and, therefore, the capacitor application is not under direct control of the utility. This practice has the disadvantage that the expense of the reactor is incurred not only when it is not required but also when its omission would improve the 720-cycle operation.

As was pointed out in the paper, when capacitors give harmful 720-cycle effects, the use of reactors in series with some (not all) capacitors gives over-all beneficial effects. The other users who have encountered this trouble with one or more of their capacitors have been able to follow this more economical solution.

Because of wide variations in distribution-system characteristics and in practices followed in applying capacitors, it is impossible to make universally applicable predictions as to the probable number of capacitors giving harmful effects which cannot be overcome by proper adjustment of the transmitter. Assuming 720-cycle carrier is used on all distribution circuits within its field of application, studies and operating experience indicate that, in general, reactors would be required in not more than ten per cent of the secondary capacitors and not more than 15 per cent of the primary feeder capacitors present on these circuits.

Operating experience so far indicates that the application data quite properly include conservative margins to allow for unknown factors in the applications. As these unknown factors become better understood through additional operating experiences, it is probable that these margins can be reduced to obtain economic savings.

Report of the Board of Directors

THE BOARD OF DIRECTORS of the American Institute of Electrical Engineers presents herewith to the membership its 59th annual report, for the fiscal year ending April 30, 1943. A general balance sheet showing the condition of the Institute's finances on April 30, 1943, together with other detailed financial statements, is included herein. This report contains a brief summary of the principal activities of the Institute during the year, more detailed information having been published from month to month in *Electrical Engineering*.

BOARD OF DIRECTORS' MEETINGS

The board of directors held five meetings during the year, four in New York, N. Y., and one in Chicago, Ill.

Information regarding many of the more important activities of the Institute which have been under consideration by the board of directors and the committees is published each month in the section of *Electrical Engineering* devoted to Institute activities.

WAR ACTIVITIES

The Institute has continued its general co-operation in many undertakings connected with the war efforts. In accordance with recommendations of the committee on planning and co-ordination, the board of directors approved, on August 7, 1942, a comprehensive statement of Institute policies and activities under war conditions. It provides for the holding of national technical meetings and District technical meetings, with the omission of all programmed social activities, including dinner dances, ladies program, and those trips which do not form a part of the technical program.

Technical papers, publications, and Section meetings are to be devoted to subjects closely related to the war efforts in so far as practicable. Under the sponsorship of the standards committee, the technical committees are reviewing many of the AIEE Standards in the light of wartime requirements. A considerable number of guides for the economical application and operation of electrical apparatus are in process of development by the technical committees under the sponsorship of the technical program and standards committees. These will supplement many existing standards, and assist in the conservation of critical materials. In some cases, wartime standards may be prepared. An emergency modification of AIEE Standard 45, "Electrical Installations on Shipboard," has been completed.

All committees are expected to conduct their work in accord with the spirit of these policies, and the committee on planning and co-ordination will submit new recommendations if changes become necessary.

The committee on national defense and the committee on civil protection were combined to form the committee on co-operation with war agencies, and its services were offered by President Osborne to the several government agencies most likely to need such assistance as it might be able to render.

This committee has rendered especially valuable assistance to the armed forces in their search for technical specialists qualified for commissions. More than 600 names were submitted. It appointed three subcommittees: one to collect and distribute information on conservation of critical materials, one to serve in an advisory capacity on the allocation of technical man power, and the third to assist in building up a technical library in the office of the chief signal officer in Washington.

A special committee was appointed to confer with representatives of the conservation division of the War Production Board on the substitution of silver for copper and aluminum in electrical installations. It submitted a comprehensive report.

In his visits with many Sections, President Osborne has participated in programs arranged for intensive discussions of means of conserving critical materials in co-operation with representatives of the War Production Board.

A great many Sections have held meetings connected with various phases of the war efforts, including a considerable number of production clinics intended to expedite production of war materials, held in co-operation with other engineering societies. Sections have also co-operated in local war activities.

Three members of the headquarters staff are serving in the armed forces: G. Ross Henninger, editor, as lieutenant-colonel in the Air Service Command, Patterson Field, Ohio; W. R. MacDonald, Jr., assistant editor, as major in the Signal Corps, Washington, D. C.; and John J. Anderson, Jr., of the standards department, in the U. S. Naval Reserve, receiving training on the USS *Prairie State,* New York, for a commission as ensign.

The headquarters staff has, of course, continued rendering all possible assistance to the armed forces and divisions of government in response to various types of requests.

PRESIDENT'S VISITS

President Osborne attended the summer convention, the national technical meeting in January, the North Eastern District technical meeting in Pittsfield, Mass., and the South West District technical meeting in Kansas City. In June he will attend the national technical meeting in Cleveland, Ohio.

Places Visited by President Osborne

California
Los Angeles Section
San Diego Section
San Francisco Section

Colorado
Denver Section

District of Columbia
Washington Section

Georgia
Georgia Section, Atlanta

Illinois
Chicago Section

Indiana
South Bend Section

Kansas
Wichita Section

Massachusetts
Boston Section
Worcester Section
North Eastern District technical meeting, Pittsfield

Missouri
South West District technical meeting, Kansas City

Nebraska
Nebraska Section, Omaha

New York
National technical meeting, New York
Niagara Frontier Section, Buffalo
Rochester Section
Syracuse Section

North Carolina
North Carolina Section, Raleigh

Ohio
Akron Section
Cincinnati Section
Cleveland Section

Oklahoma
Oklahoma City Section

Oregon
Portland Section

Pennsylvania
Philadelphia Section
Pittsburgh Section

Virginia
Virginia Section, Richmond

Washington
Seattle Section

Wisconsin
Milwaukee Section

Canada
Saskatchewan Section, Regina
Toronto Section
Vancouver Section

ANNUAL MEETING

The annual business meeting of the Institute was held Monday morning, June 22, 1942. The annual report of the board of directors for the fiscal year which ended April 30, 1942, was presented in abstract by the national secretary. A report on the finances of the Institute was presented by National Treasurer W. I. Slichter. The report of the committee of tellers upon the election of officers for the year beginning August 1, 1942, was presented, and President-Elect Osborne responded to his introduction with a brief address. During this session there was an address by D. D. Ewing, head, school of electrical engineering, Purdue University, Lafayette, Indiana. Also, national prizes for papers presented in 1941 were presented, and the Lamme Medal for 1941 was presented to Forrest E. Ricketts, vice-president, Consolidated Gas Electric Light and Power Company of Baltimore.

NATIONAL CONVENTIONS

Three national conventions were held during the year, and a brief report on each follows:

Summer Convention. The 58th summer convention was held in Chicago, Ill., June 22–26, 1942. In addition to the annual business meeting, and conference of officers, delegates, and members, there were 20 sessions and conferences with approximately 60 technical papers and informal conference presentations. A general session on the subject of "The Organization and Management of Large-Scale Engineering Works" was addressed by M. R. Sullivan, vice-president, American Telephone and Telegraph Company, New York, N. Y.; R. C. Muir, vice-president, General Electric Company, Schenectady, N. Y.; and H. B. Gear, vice-president, Commonwealth Edison Company, Chicago, Ill. Entertainment features consisted of a preconvention reception for early arrivals, president's reception and dancing, buffet dinner at the Lake Shore Club, banquet, and golf tournaments. The registration was 1,179.

Pacific Coast Convention. The 30th Pacific Coast convention was held in Vancouver, B. C., Canada, September 9–11, 1942, with a registration of 245. "Wartime National Efficiency" was the topic of an address given by G. A. Gaherty, president, Montreal Engineering Company, Ltd., and Walter J. Gilson, general manager, Eastern Power Devices, Ltd., Toronto, gave an address entitled "International Prejudices" before a joint luncheon meeting sponsored by the Vancouver Electric Club. There were five technical sessions and one student session. Social features included a luncheon party, a musical program, a bridge tea, and a formal dance. The 22nd annual competition for the John B. Fisken Golf Trophy was won by Bernard DeM. Mertens, British Columbia Electric Railway Company, Ltd.

National Technical Meeting. The 31st national technical meeting (formerly called winter convention) was held in New York, January 25–29, 1943, with a program including 13 technical sessions and 12 conferences, at which 62 papers were presented. At a general session, the two feature addresses concerned the specialized technical training programs established by the Army and Navy in collaboration with leading educational authorities. Colonel Herman Beukema, director of specialized training division, United States Army, and J. W. Barker, special assistant to the secretary of the Navy, were the speakers. Discussions were by R. E. Doherty, president, Carnegie Institute of Technology, Pittsburgh, Pa.; R. C. Muir, vice-president, General Electric Company, Schenectady, N. Y.; and George T. Seabury, secretary, American Society of Civil Engineers.

At a dinner meeting, Vannevar Bush, president, Carnegie Institution of Washington, and director of the Office of Scientific Research and Development, spoke on "Research in the War Effort." At a session on Wednesday evening three medals were presented: the Edison Medal to Edwin H. Armstrong, the John Fritz Medal to Willis R. Whitney, and the Hoover Medal to Gerard Swope. Social events and inspection trips were omitted in accordance with the wartime policies adopted by the board of directors. The registration was 1,419.

Concurrently with the AIEE national technical meeting, the Institute of Radio Engineers held its winter conference, which culminated in a joint meeting at which George C. Southworth, Bell Telephone Laboratories, Inc., spoke on "Beyond the Ultra Shorts." Following this, there were talks by Sir Noel Ashbridge, chief engineer, British Broadcasting Company, by shortwave radio from London; L. P. Wheeler, president IRE, Arthur F. Van Dyck, IRE past president, and James Lawrence Fly, chairman of the Board of War Communications, who spoke from Washington, D. C.

DISTRICT MEETINGS

North Eastern District Meeting. This meeting was held in Pittsfield, Mass., April 8–9, 1943, and included five conference sessions, a general session, and three technical sessions. At a general session there were addresses by H. S. Osborne, president AIEE, and J. F. Burt of the Western Massachusetts Companies. The get-together dinner was followed by an address by C. E. Smith, vice-president, New York, New Haven, and Hartford Railroad, on "Railroad Transportation in the Present Emergency." The registration was 319.

South West District Meeting. The ninth meeting of the South West District was held in Kansas City, Mo., April 28–30, 1943. It was the third held in that city. The principal features were a general session and a symposium on critical materials and equipment, both held jointly with the Missouri Valley Electric Association, four technical sessions, one student session, a luncheon with three talks, a district executive committee meeting, a clinic on Section management and operation, a dinner for men, and arrangements for student consultations with industry representatives. The attendance was 570, including 338 AIEE members, students, and guests.

Table I. Section and Branch Statistics

	For Fiscal Year Ending April 30					
	1938	1939	1940	1941	1942	1943
Sections						
Number of Sections	65	67	70	72	72	73
Number of meetings held	624	635	701	703	647	598
Total attendance	110,148	85,692	91,949	92,554	78,254	66,111
Branches						
Number of Branches	120	120	121	123	124	125
Number of meetings held	1,334	1,190	1,346	1,163	946	942
Total attendance	60,446	53,380	64,972	52,285	37,785	38,227

SECTIONS

1. Programs Connected With War Efforts. The Sections committee sent three letters, August 28, September 29, and January 15, to each Section to encourage participation in war efforts. All Sections have a keen appreciation of the importance of this contribution to the war effort. A great many programs have been devoted to conservation of critical materials, production clinics, and other subjects connected with the war.

2. Development of Central and South American Sections. In response to the committee's inquiries among members in Central and South America regarding the desirability of forming Sections in their countries, many favorable letters have been received. Certain difficulties appear to make the formation of regular Sections impracticable at present, but the committee is planning to recommend to the board of directors a plan for a special type of organization for those countries.

3. Section Territory in the United States. The only remaining areas in the United States not assigned to Sections are in Arkansas and Nevada. The committee and the vice-presidents of Districts 8 and 9 are recommending the assignment of the remaining counties in Nevada to three Sections.

4. Subsections. The committee wrote each Section on December 10 regarding the desirability of forming geographical Subsections and on February 26 regarding the formation of technical groups. A new folder on Subsections was prepared, and copies were sent to the Sections in March. Some Subsections have been formed recently, and others are under consideration.

5. Contact With Members in Armed Forces. The Sections committee was actively interested in the development of recommendations upon which the board of directors acted in January, providing an inactive membership status for members and enrolled students in the armed forces and the merchant marine, and authorizing the publication committee to prepare for mailing to them a quarterly news letter.

6. Section Operation and Management. Special plans have been developed for discussions on the management of Section activities and the types of information that should be supplied to the Sections.

STUDENT BRANCHES

One new Student Branch was established during the year, at Vanderbilt University, bringing the total number to 125.

The interest of the students in Institute activities has continued very satisfactorily. There is apparently some increase in the use of outside speakers and in the use of industrial moving pictures for meetings. It seems that industrial moving pictures may assist in replacing inspection trips. On the whole, it seems that the Canadian Branches have maintained a greater degree of normalcy than the branches in this country in regard to meetings and inspection trips.

Some of the District group meetings of the Branches have been given up because of the accelerated programs in colleges and transportation difficulties.

With the increasing use of staff and facilities by the Army and Navy, together with the strict scheduling of the student time in the Army and Navy, the work of the Student Branches may be severely handicapped. It is hoped that the enthusiasm and diligence of the counselors may inspire the students whether in civilian, Army, or Navy life, so that the Branch activities may be continued. There exists a real challenge to bring to the future engineers a definite view of peacetime engineering and of the ideals of the profession which will stand them in good stead as the havoc of war decreases.

General Committees

FINANCE COMMITTEE

A continuance in the growth of Institute membership, together with a material increase in revenue from advertising in *Electrical Engineering*, permits the finance committee to report for the fiscal year an excess of receipts over expenditures of approximately $38,000, in addition to transfers made during the year of $10,000 to the pension fund, and $20,000 to the reserve capital fund.

In its preparation of the annual budget, the finance committee has endeavored to give full consideration to probable losses in dues revenue as a result of concessions now being made to members serving in the armed forces and merchant marine.

Expenditures have been in accordance with a program of Institute activities in effect during the past few years except as modified by any special conditions adopted to conform with the war effort. Particulars regarding the provisions of the current budget appear in the December 1942 issue of *Electrical Engineering* news section, pages 617–18.

Institute investments now reflect the current market trend. Upon referring to the financial statement accompanying this report, it will be noted that losses on securities which developed in less favorable years have been almost entirely overcome by the appreciation in other security values. Approximately 55 per cent of the Institute's investments is now in United States war and other Government bonds.

It is expected that losses in dues revenue resulting from concessions to members in service will become more evident during the fiscal year which began May 1, 1943, and that the cost of Institute activities will increase by reason of war conditions; for example, the executive committee of the Institute recently voted to increase the financial support of United Engineering Trustees, Inc., because of increased operating expenses and a decrease in normal building-operating revenue. For this reason, the committee expects that some of the recent excess of receipts over expenditures will be needed to meet deficiencies and increased costs.

Haskins & Sells, certified public accountants, have audited the Institute books and their report appears on pages 13 to 16.

TECHNICAL PROGRAM COMMITTEE

Technical Meetings. Three national and two District technical meetings, comprising 44 technical sessions and 24 conferences, were held during the year. The total registration at the five meetings was 3,497, as compared with 4,274 at six meetings held in 1941–42. The registration at the three national technical meetings was 2,843, as compared with 2,856 the year before, despite a large decrease in guest registration. At the national technical meeting in January, the average attendance at each of the 29 sessions and conferences was 138, compared with an average attendance of 109 at each of the 27 sessions held last year, and 121 at each of the 27 sessions in 1941.

Programs. In line with the AIEE wartime policy (*EE, Sept. 1942, p. 477*), the committee gave preference to subjects of immediate importance in the war effort, and fully one third of the sessions were devoted wholly to wartime problems. A principal theme of the year's work was the promotion of "Conservation of Critical Materials Through Good Engineering" through the presentation and discussion of a series of wartime guides or reports and papers on the selection and operation of electrical equipment (*EE, Oct. 1942, p. 527*). Twelve such guides or reports were prepared or proposed (*EE, Mar. 1943, p. 114*). It was agreed that these guides should be entirely educational in character, and that they should all be reconsidered, and approved, revised for normal use, or rescinded, after the war emergency. A foreword to this effect, prepared by the standards committee, was included in all such reports prepared subsequent to April 1943.

Civic Affairs Conferences. With a view to furthering the participation of engineers in civic affairs, especially city and regional planning, conferences were held at the Chicago and Pittsfield meetings. The Pittsfield conference, held during the North Eastern District technical meeting, on April 8, brought together six civic authorities and experts who are actively engaged in planning work. Their addresses followed an address by President Osborne on "The Engineer's Interest in City Planning." A similar conference has been proposed for the national technical meeting to be held in Cleveland.

Committee Policies. To minimize time and travel, the committee held only three meetings during the year. These three meetings were attended by 56, 50, and 66 per cent of the committee members or duly appointed representatives.

In view of the increasing importance of electronics, the committee recommended and the board of directors approved giving the joint subcommittee on electronics full status as a technical committee. Because of the expanding use of carrier current for multiple purposes, which are in the fields of several technical committees, a joint subcommittee on power system applications of carrier current has been appointed consisting of representatives of the committees on protective devices, automatic stations, power transmission and distribution, and communication.

To aid the war effort and to encourage reports on the latest developments, restrictions on the closing dates for the receipt of papers have been relaxed. This, together with the preoccupation of most authors and reviewing committee members with war work, has led to an increase in the length of the average paper to a figure of 6.28 pages, the highest figure in the past five years.

General Sessions. General sessions have dealt with the broader aspects of the profession as related to the war effort and postwar problems. The general session for the meeting in Chicago was on the subject of "The Organization and Management of Large-Scale Engineering Work." The general session of the January meeting was devoted to "Technical Man Power in the War Effort." Plans are being made to devote the general session of the meeting in Cleveland to the engineering aspects of Latin-American relationships.

PUBLICATION COMMITTEE

The Institute's principal technical publications were produced during the year on the same general basis as during the preceding year, no change in publication

Table II. Statistics of Technical Programs for Last Five Years

	Year Ending April 30, 1943	Year Ending April 30, 1942	Year Ending April 30, 1941	Year Ending April 30, 1940	Year Ending April 30, 1939
Number of national technical meetings	3	3	3	2	3
Number of District technical meetings	2	3	2	3	3
Registration at national meetings and District meetings	3,497	4,274	4,339	3,548	4,100
Number of papers presented	168	182	195	177	184
Number of papers recommended for *Transactions*	141	152	167	151	169
Estimated number of pages required for printing papers in *Transactions*	886*	807*	938*	886*	986*
Average length of papers recommended for *Transactions*	6.28	5.3	5.72	5.86	5.83
Number of technical sessions	44	50	46	43	50
Number of technical conferences	24	23	18	10	12

* Partly estimated.

Table III. Membership Statistics for Fiscal Year Ending April 30, 1943

	Honorary Members	Fellows	Members	Six-Year Associates	Associates	Subtotals	Totals
Membership April 30, 1942	5	792	4,903	6,145	7,099		18,944
Additions							
New members qualified		1	116		1,628	1,745	
Fomer members re-instated or re-elected		2	79	51	105	237	
Subtotals		3	195	51	1,733	1,982	
Transfers	1	48	250	771		1,070	
Totals	1	51	445	822	1,733	3,052	
Deductions							
Died		15	51	50	13	129	
Resigned		3	27	112	80	222	
Dropped		2	34	152	226	414	
Subtotals		20	112	314	319	765	
Transfers			48	216	806	1,070	
Totals		20	160	530	1,125	1,835	
Net Changes	1	31	285	292	608	1,217	
Membership April 30, 1943	6	823	5,188	6,437	7,707		20,161

policy having been enacted in the interim.

The 1942 *Transactions* volume marked the completion of the transition to the improved publication procedure adopted in August 1940, and included only 1942 technical papers and related discussions. Publication of technical papers continues on a month-by-month basis, in a segregated *Transactions* section of *Electrical Engineering*, with discussions and a few long technical papers published in the two semiannual "Supplements to *Electrical Engineering—Transactions* Section."

Subject matter of both technical papers and general-interest articles published in *Electrical Engineering* is now closely related to the war effort, in accordance with the wartime policies adopted by the Institute's board of directors in August 1942. First preference is given those papers and articles directly aiding the war effort, and those not meeting this requirement constitute a minor part of the total program.

Two factors are now limiting the amount of technical material published:

1. A reduced amount of available material, probably a result of the employment of the majority of electrical engineers in war work that cannot be publicized.
2. Governmental restrictions on the use of paper.

However, efforts are being made to maintain the publication of technical material at the highest possible level.

In addition to the usual technical-paper pamphlets produced for national and District meeting programs, special pamphlets which were published in the year included:

1. "Bibliography on Circuit Interrupting Devices, 1928–40," prepared by the AIEE committee on protective devices, May 1942.
2. "Bibliography on Electrical Safety, 1930–41," prepared by the AIEE committee on safety, July 1942.
3. "Bibliography on Automatic Stations, 1930–41," prepared by the AIEE committee on automatic stations, December 1942.
4. "Advanced Methods of Mathematical Analysis as Applied to Electrical Engineering." This is a consolidated reprint of five lectures published in *Electrical Engineering*, originally presented as a symposium under auspices of the basic science group of the New York Section.

In addition to the special publications already mentioned, the board of directors at its January 1943 meeting authorized the publication of a news bulletin for AIEE members in the armed services and the merchant marine. The first issue is now in preparation.

Governmental censorship regulations have continued in effect as reported last year. Measures have been taken to avoid publication of censorable material, and manuscripts dealing with restricted subject matter have been cleared with designated authorities before publication. Shipments of publications to members and subscribers in foreign countries have continued on the same basis as reported last year. No shipments are made to enemy territory or to enemy-occupied or controlled territory. Distribution to other foreign destinations is being made under export-license control and rigorous limitations imposed by the United States Office of Censorship and the Office of Export Control of the Board of Economic Warfare.

MEMBERSHIP COMMITTEE

The Institute membership stood at 20,161, April 30, 1943. This compares with a membership of 18,944 on April 30 last year. The goal set by the membership committee was 20,000. It was never quite clear, because of the abnormal conditions which prevailed, whether this objective would be attained. There have been many mixed influences throughout the past year resulting from the readjustments of all engineers to war conditions. It is a tribute to the loyalty and determination of every Section membership committeeman that we have been able to forge ahead at all when almost every reasonable sign pointed in the reverse direction. As in past years our best source of leads for new members was the very fine response to the chairman's letter of September 16, 1942, to the entire membership. Some 896 names of candidates were submitted. The membership committee herewith expresses its gratitude to those members who sent in suggestions for new members.

As usual several tables have been included to show what has been happening to the Institute membership, comparing this year's results with those of previous years.

In Table III it is seen that the gross additions to membership were 1,982 while the deductions were 765. The net increase in membership was 1,217. Last year the net increase amounted to 1,058 members. This year's net increase of 1,217 members

Table IV. Number of Applications Received From Enrolled Students and From All Others

Year Ending April 30	Students	All Others	Total
1943	783	1,431	2,214
1942	971	1,031	2,002
1941	887	1,011	1,898
1940	911	918	1,829
1939	849	872	1,721

Table V. Number of Enrolled Students as of April 30

Year	New Applications	Renewals	Total
1943	2,512	3,200	5,712
1942	2,585	3,377	5,962
1941	2,351	3,188	5,539
1940	2,525	2,992	5,517
1939	2,271	2,971	5,242

Table VI. Number of Delinquent Members in Section Territory Reinstated

August 1, 1942 to April 30, 1943	323
Year beginning August 1, 1941	318
Year beginning August 1, 1940	383
Year beginning August 1, 1939	302
Year beginning August 1, 1938	354

Table VII. Status of Membership Dues as of April 30

Year	Total Membership	Members Fully Paid: Number	Members Fully Paid: Per Cent
1943	20,161*	18,247**	90.5
1942	18,944	16,922	89.3
1941	17,886	15,777	88.2
1940	17,213	14,997	87.1
1939	16,605	14,371	86.5

* Of the 20,161 members reported for April 30, 1943, 771 names are being carried upon the membership rolls in an inactive status, in accordance with the resolutions adopted by the board of directors in the interest of members in the military service of the United States or allied countries, or members located in such countries unable to remit membership dues at this time:

1. Members owing dues to April 30, 1942........496
2. Members owing dues to April 30, 1943......1,418

(During the period May 1 to May 17, 1943, 297 members have paid dues to April 30, 1943, reducing the number not fully paid to 1,121.)

** Including 936 Members for Life; and the 771 mentioned above who are not currently paid up, but from whom no payments are expected during the war.

is the largest ever recorded except for the year ending April 30, 1921.

Table IV indicates the number of applications received from enrolled students and others for the past five years. The number of applications from enrolled students is lower than that reported last year, principally because those in the armed forces and merchant marine were given the opportunity to continue their student affiliation while in service. The increase of 400 applications this year as compared with last year from other than students reflects wider employment of engineers and a general increased need on the part of engineers to keep abreast of latest developments in the field of electrical engineering.

It should be pointed out that no direct check can be made between Tables III and IV, because applications received are not all acted on before the close of the fiscal year.

Table V shows data on the number of enrolled students and new applications for student membership from students for five years ending 1943. The effect of the war is very clearly indicated in the figure for 1943.

The results portrayed in Table VI show in no small way the success achieved by Section membership committeemen in their contacts with wavering members.

Table VII contains the "vital" statistics of the Institute for this year as compared with the past four years. Since it is the "fully paid members" who make the AIEE the successful organization that it is, it is always of interest to have a look at their statistical behavior. The total membership of 20,161 on April 30, 1943, includes 771 names being carried in an inactive status because of military service as compared with 327 names on the same date last year.

Table VIII. Record of AIEE Membership

Year	Total May 1	Year	Total May 1	Year	Total May 1
1884	71	1905	3,460	1925	17,319
1885	209	1906	3,870	1926	18,158
1886	250	1907	4,521	1927	18,344
1887	314	1908	5,674	1928	18,265
1889	333	1909	6,400	1929	18,133
1890	427	1910	6,681	1930	18,003
1891	541	1911	7,117	1931	18,334
1892	615	1912	7,459	1932	17,550
1893	673	1913	7,654	1933	17,019
1894	800	1914	7,876	1934	15,230
1895	944	1915	8,054	1935	14,269
1896	1,035	1916	8,202	1936	14,600
1897	1,073	1917	8,710	1937	15,308
1898	1,098	1918	9,282	1938	16,078
1899	1,133	1919	10,352	1939	16,605
1900	1,183	1920	11,345	1940	17,213
1901	1,260	1921	13,215	1941	17,886
1902	1,549	1922	14,263	1942	18,944
1903	2,229	1923	15,298	1943	20,161
1904	3,027	1924	16,455		

Table VIII is included to keep the record straight and is self-explanatory.

It would be easier to prepare a report of the year's membership activities if Table IX could be left out. Every year we note

Table IX. Deaths of AIEE Members Reported in "Electrical Engineering"

Name	Date of Election	Date of Death	Grade at Death	Obituary Notice in Electrical Engineering
Abeel, L. B.	Associate '31	Oct. 20, 1942	Associate	Feb. 1943, p. 77
Ackland, E. W.	Associate '05	Mar. 28, 1942	Member	July 1942, p. 366
Allner, F. A.	Associate '12	July 18, 1942	Member	Sept. 1942, p. 481
Ames, Azel	Associate '09	Nov. 23, 1942	Associate	Jan. 1943, p. 35
Andree, J. W.	Member '26	Apr. 7, 1942	Member	July 1942, p. 366
Atwood, J. A.	Associate '16	Jan. 7, 1942	Associate	July 1942, p. 366
Barry, J. G.	Associate '03	Mar. 4, 1943	Associate	Apr. 1943, p. 177
Barstow, W. S.	Associate '94	Dec. 26, 1942	Fellow	Feb. 1943, p. 76
Baylis, R. N.	Associate '89	Sept. 5, 1942	Member	Mar. 1943, p. 132
Booth, G. A.	Associate '13	Feb. 18, 1942	Associate	May 1942, p. 264
Brenner, W. H.	Associate '93	June 20, 1942	Member	Sept. 1942, p. 481
Brown, J. E.	Associate '03	Aug. 23, 1942	Associate	Nov. 1942, p. 578
Calmus, F. A.	Associate '29	Aug. 2, 1942	Associate	Oct. 1942, p. 533
Carpenter, C. E.	Associate '96	Nov. 9, 1942	Associate	Jan. 1943, p. 35
Christiansen, K. C.	Associate '17	Dec. 5, 1942	Member	Mar. 1943, p. 131
Clark, H. D.	Associate '32	Jan. 21, 1942	Associate	June 1942, p. 331
Coho, H. B.	Associate '94	May 19, 1942	Associate	Dec. 1942, p. 624
Cole, G. M.	Associate '02	Jan. 6, 1942	Associate	Aug. 1942, p. 438
Constad, H.	Associate '35	Sept. 1940	Associate	Oct. 1942, p. 533
Cornell, C. L.	Associate '90	July 18, 1942	Member	Sept. 1942, p. 481
Cranston, J. A.	Associate '08	Apr. 15, 1942	Associate	June 1942, p. 330
Danford, F. E.	Associate '37	Oct. 11, 1942	Associate	Dec. 1942, p. 624
Darling, N. J.	Member '34	Oct. 26, 1942	Member	Dec. 1942, p. 624
Davenport, J. C.	Associate '07	Dec. 19, 1942	Member	Mar. 1943, p. 132
Dodson, H. A.	Associate '37	Jan. 23, 1942	Associate	June 1942, p. 330
Dow, Alex	Associate '93	Mar. 22, 1942	Honorary Member	May 1942, p. 263
Durran, R. T.	Associate '05	June 5, 1942	Associate	Sept. 1942, p. 482
Eaton, R. W.	Associate '07	June 1, 1942	Member	Aug. 1942, p. 438
Edwards, J. P.	Associate '92	July 10, 1942	Member	Nov. 1942, p. 578
Egli, J.	Associate '26	May 10, 1942	Member	Aug. 1942, p. 438
Fenley, W. H.	Associate '14	Nov. 22, 1942	Associate	Mar. 1943, p. 132
Flory, A. C.	Member '26	Mar. 1, 1943	Member	Apr. 1943, p. 178
Fortenbaugh, S. B.	Associate '95	Feb. 6, 1943	Fellow	Mar. 1943, p. 131
Freeman, W. E.	Associate '11	May 21, 1942	Member	July 1942, p. 365
Frost, F. G.	Member '22	July 1, 1942	Member	Oct. 1942, p. 533
Gallagher, J. A.	Associate '42	Nov. 11, 1942	Associate	Apr. 1943, p. 178
Glander, E. G.	Associate '11	Sept. 10, 1942	Associate	Nov. 1942, p. 578
Goldenstein, M. M.	Associate '07	Oct. 1942	Member	Mar. 1943, p. 32
Goldmark, C. J.	Associate '88	Nov. 3, 1942	Associate	Feb. 1943, p. 77
Hadley, F. W.	Associate '96	June 7, 1942	Member	Dec. 1942, p. 624
Harding, C. F.	Associate '06	Apr. 13, 1942	Fellow	May 1942, p. 264
Harisberger, John	Associate '02	Sept. 9, 1942	Member	Dec. 1942, p. 623
Hays, J. C.	Associate '04	July 21, 1942	Member	Sept. 1942, p. 481
Hayward, H. O. F.	Associate '39	Jan. 15, 1943	Associate	Apr. 1943, p. 178
Hellmund, R. E.	Associate '05	May 16, 1942	Fellow	June 1942, p. 329
Herpich, R. L.	Associate '42	July 30, 1942	Associate	Nov. 1942, p. 578
Hertz, S. S.	Associate '17	Feb. 20, 1943	Fellow	Apr. 1943, p. 176
Hewitt, W. R.	Associate '94	Feb. 21, 1940	Associate	Aug. 1942, p. 439
Hilbert, E. E.	Associate '42	Feb. 26, 1943	Associate	Apr. 1943, p. 178
Holmes, J. T.	Member '24	Feb. 12, 1943	Member	Apr. 1943, p. 178
Hosford, H. B.	Associate '30	Dec. 1942	Associate	Mar. 1943, p. 133
Jarvis, C. D.	Associate '11	Mar. 6, 1942	Associate	May 1942, p. 264
Kallir, Ludwig	Member '29	Jan. 7, 1943	Member	Apr. 1943, p. 178
Kearns, E. W.	Associate '24	Feb. 18, 1942	Associate	July 1942, p. 366
Kelly, P.	Associate '19	Apr. 5, 1942	Associate	July 1942, p. 366
Koppitz, C. G.	Member '21	Jan. 6, 1943	Member	Feb. 1943, p. 76
Lang, R. H.	Associate '21	Apr. 4, 1942	Member	June 1942, p. 330
Larner, C. W.	Associate '12	June 11, 1942	Associate	Aug. 1942, p. 438
Manson, D. E.	Associate '07	Apr. 30, 1942	Member	July 1942, p. 365
McBerty, F. P.	Associate '12	Oct. 19, 1942	Associate	Mar. 1943, p. 132
Meyer, D. C.	Member '29	Feb. 2, 1943	Member	Apr. 1943, p. 177
Morgan, M. I.	Associate '39	Jan. 6, 1943	Associate	Mar. 1943, p. 132
Morrow, L. W. W.	Associate '13	Nov. 15, 1942	Fellow	Dec. 1942, p. 623
Noble, C. S.	Associate '18	Sept. 17, 1942	Member	Feb. 1943, p. 77
Nordstrum, L. D.	Associate '04	Feb. 1, 1943	Member	Apr. 1943, p. 178
O'Reilly, A. J.	Associate '03	Jan. 27, 1943	Associate	Apr. 1943, p. 176
O'Toole, J. R.	Associate '39	Apr. 6, 1942	Associate	Mar. 1943, p. 133
Outzen, A. N.	Associate '14	Aug. 26, 1941	Associate	Apr. 1943, p. 176
Palmer, J.	Associate '13	Apr. 20, 1942	Associate	July 1942, p. 366
Penn, Marion	Member '21	Mar. 29, 1942	Fellow	May 1942, p. 264
Phillips, F. R.	Member '27	Oct. 23, 1942	Member	Dec. 1942, p. 623
Porter, J. F.	Associate '87	Nov. 7, 1942	Fellow	Dec. 1942, p. 623
Porter, J. S.	Associate '35	May 2, 1942	Associate	Oct. 1942, p. 533
Prindle, E. J.	Associate '06	Dec. 17, 1942	Member	Feb. 1943, p. 77
Pyper, D.	Associate '29	Mar. 6, 1942	Associate	Sept. 1942, p. 482
Quigley, F. P.	Member '33	Mar. 22, 1942	Member	May 1942, p. 264
Reiber, A. H.	Associate '21	Feb. 1, 1943	Fellow	Apr. 1943, p. 177
Reist, H. G.	Associate '90	July 5, 1942	Fellow	Sept. 1942, p. 481
Rice, H. F.	Associate '21	Mar. 24, 1942	Member	June 1942, p. 330
Roff, O. C.	Associate '07	Mar. 25, 1942	Associate	June 1942, p. 330
Rosebrugh, T. R.	Associate '91	Jan. 24, 1943	Associate	Apr. 1943, p. 177
Ruess, C. N.	Associate '25	Sept. 7, 1942	Associtae	Jan. 1943, p. 35
Runyon, W. M.	Associate '41	June 1942	Associate	Mar. 1943, p. 132
Rutherford, E. J.	Member '35	Dec. 10, 1942	Member	Mar. 1943, p. 132
Sanderson, E. W.	Associate '10	Mar. 1942	Associate	July 1942, p. 366
Sandstrom, P. N.	Associate '25	Jan. 14, 1943	Associate	Mar. 1943, p. 132
Sasscer, W. H.	Associate '28	Apr. 28, 1942	Member	July 1942, p. 366
Schiff, Martin	Member '20	Feb. 15, 1943	Fellow	Apr. 1943, p. 177

(Continued on following page)

Table IX (continued). Deaths of AIEE Members Reported in "Electrical Engineering"

Name	Date of Election	Date of Death	Grade at Death	Obituary Notice in Electrical Engineering
Schwarz, E. H.	Associate '03	Feb. 21, 1943	Member	Apr. 1943, p. 177
Scudder, Hewlitt	Associate '99	July 31, 1942	Associate	Sept. 1942, p. 481
Seelig, A. E.	Associate '07	Sept. 13, 1942	Associate	Feb. 1943, p. 77
Sharp, C. H.	Associate '02	May 14, 1942	Fellow	June 1942, p. 329
Shreeve, H. E.	Associate '06	Apr. 24, 1942	Fellow	June 1942, p. 330
Siegel, E. M.	Member '42	Apr. 10, 1942	Member	June 1942, p. 330
Smith, A. C.	Member '15	Oct. 25, 1942	Member	Mar. 1943, p. 131
Snook, H. C.	Associate '05	Sept. 23, 1942	Fellow	Nov. 1942, p. 578
Spurck, R. M.	Associate '18	Dec. 12, 1942	Fellow	Feb. 1943, p. 77
Stahl, N.	Associate '08	Jan. 1, 1943	Fellow	Mar. 1943, p. 131
Stewart, R. S.	Associate '96	Nov. 11, 1942	Member	Apr. 1943, p. 177
Stockwell, J. F.	Associate '03	Jan. 28, 1943	Associate	Apr. 1943, p. 177
Summers, C. L.	Associate '41	1942	Associate	Mar. 1943, p. 133
Tanzer, E. D.	Associate '15	June 11, 1942	Associate	Oct. 1942, p. 533
Tesla, Nikola	Associate '88	Jan. 7, 1943	Fellow	Feb. 1943, p. 76
Townsend, C. W.	Associate '41	May 28, 1942	Associate	July 1942, p. 366
Towson, M. S.	Associate '08	Mar. 17, 1942	Associate	June 1942, p. 330
VanDyke, C. S.	Associate '11	May 8, 1942	Associate	Aug. 1942, p. 439
Ward, Ralph B.	Associate '24	July 26, 1942	Member	Jan. 1943, p. 36
Weber, R. L.	Associate '16	Apr. 27, 1942	Member	Aug. 1942, p. 438
White, J. G.	Associate '89	June 2, 1942	Member	July 1942, p. 365
Whittle, Horace	Associate '20	Feb. 18, 1942	Associate	May 1942, p. 264
Williams, J. H.	Associate '13	July 1942	Member	Dec. 1942, p. 624
Woltz, F. I.	Associate '10	Nov. 27, 1942	Member	Feb. 1943, p. 77
Wood, L. A. S.	Member '24	May 5, 1942	Member	July 1942, p. 365
Wood, W. D.	Associate '31	Mar. 22, 1942	Associate	June 1942, p. 330
Woodworth, L. B.	Associate '00	May 29, 1942	Member	Oct. 1942, p. 533
Zimmer, C. S.	Associate '37	July 21, 1942	Associate	Nov. 1942, p. 578

with sadness the name of a friend, associate, or acquaintance who has left our ranks forever. Again this year we pay our last humble respects to those of our membership who have taken their departure from this life.

The general organizational form of the national membership committee remained the same as in the previous year. It has been found that an advisory subcommittee which assists the chairman in making quick decisions and the research subcommittee for studying perplexing problems in a detached and deliberate manner have greatly assisted in keeping the work of the committee moving in a well-balanced manner.

Table X. Applications for Admission and Transfer

Applications for Admission		
Recommended for grade of Associate	1,844	
Re-elected to the grade of Associate	136	
Not recommended	2	
		1,982
Recommended for grade of Member	197	
Re-elected to the grade of Member	26	
Not recommended	39	
		262
Recommended for grade of Fellow	1	
Re-elected to the grade of Fellow	1	
		2
Applications for Transfer		
Recommended for grade of Member	272	
Not recommended for grade of Member	13	
		285
Recommended for grade of Fellow	47	
Not recommended for grade of Fellow	—	
		47
Students		
Recommended for enrollment as Students		2,317
Total		4,895

The research subcommittee is putting together and boiling down a considerable amount of material which will ultimately form the basis for a new AIEE booklet which can be used to good advantage in explaining the Institute to potential new members. It will also be a very valuable reference booklet for each member. It is believed that a new and different way has been found to show what the Institute is and what it has done and can do for electrical engineers.

The progress on this booklet has not been so fast as we had hoped it would be because of the pressure of war work on the members of the research subcommittee. However, when the booklet becomes available, it will be well worth the wait.

It has been the consistent policy of the chairman this year to assist the Section membership committee chairmen in the principal task of getting new members and keeping present members. Relatively few communications have been sent out and those that did go out were brief and in line with the main objectives of the Committee. The Section membership committees have functioned splendidly this year and deserve credit for doing a good job in the face of many odds.

BOARD OF EXAMINERS

The board of examiners held 11 meetings during the past year, averaging about $2^1/_2$ hours each, and acted upon 4,895 cases, divided as shown in Table X. This represents an increase of 33 per cent over the total handled by the examiners during the previous year.

While there were no changes during the year in the basic procedure followed in the consideration of cases for either admission or transfer, a question was raised on the interpretation of the constitutional requirements for Fellow grade. The board of examiners has appointed a subcommittee to study this matter with a view to recommending constitutional revisions which will clarify particularly the wording of Article II, Sections 4*c*, *d*, and *e*. A second subcommittee is making a study of the admission form to determine whether a more effective form may be developed.

COMMITTEE ON PLANNING AND CO-ORDINATION

The committee considered thoroughly, in the summer of 1942, the desirability of modifying Institute activities to provide for greater assistance in the war efforts. At the meeting of the board of directors on August 7, a comprehensive statement of policies and activities of the Institute under war conditions was adopted. Some of the principal parts have been mentioned already under "War Activities." The statement was published in full (*EE, Sept. 1942, p. 477*).

The committee submitted a comprehensive report at the meeting of the board of directors January 28, 1943, which resulted in the actions outlined below:

The Sections and Districts were encouraged by the board to schedule the usual number of meetings, to be devoted to the war effort, or, in the event of a change in the international situation, to peace aims.

Paragraphs 4*c*, 4*d*, and 4*e* of article II of the constitution were interpreted as not requiring five years in Member grade.

Members and enrolled students in the armed services and the merchant marine were given the option of continuing to pay regular dues or student fees with the usual privileges, or of being inactive members or students for the duration of the war without payments.

The publication committee was authorized to prepare a quarterly news letter to be sent to members in war service.

COMMITTEE ON CODE OF PRINCIPLES OF PROFESSIONAL CONDUCT

"Canons of Ethics" were submitted by the engineers' joint committee on ethics to the Engineers Council for Professional Development board at its annual meeting in October 1942, and, after consideration by that board, were forwarded to the boards of the several constituent members of ECPD, of which AIEE is one. The submission was made with the request for

1. General comments.
2. A statement of what, if any, items of the submitted draft could be omitted appropriately from a code of ethics.
3. What, if any, items should be contained in an adequate code of ethics that are not in the submitted draft of canons.

The board of directors of AIEE referred the matter to the committee on code of principles of professional conduct, and that committee has it under consideration with the hope that a report may be submitted to the board before the summer meeting.

COMMITTEE ON CONSTITUTION AND BY-LAWS

The work of this committee has been limited to making two routine changes in the by-laws and to consideration of the desirability of making a number of changes in the constitution.

The two routine changes in the by-laws were made to make the by-laws conform with the action of the board of directors taken at its meeting on August 7, 1942, to abolish the institute policy committee.

Specifically, the changes were as follows:

Section 65—Delete the name of the institute policy committee from the listing under the heading "general committees."

Section 73—Delete the whole section.

The consideration of the desirability of making certain changes in the constitution is still in progress in the committee on constitution and by-laws, and it is expected that the committee's recommendations to the board of directors will be submitted well in advance of the close of the present administrative year.

The specific points involving the constitution which are now under consideration by the committee on constitution and by-laws are indicated by the brief titles which follow:

1. Elimination of the term "national."
2. Elimination of the term "professional engineer."
3. Tax-exempt status.
4. Election procedure.
5. Constitutional-amendment procedure.
6. Mailing amendment and election ballots together.
7. Qualifications for Fellow grade.

COMMITTEE ON RESEARCH

The third year of operation of this committee as a general committee continues to indicate that the change from technical-committee status has in no way interfered with the development of research papers by Institute members, as evidenced by a review of the large number of papers describing various research works presented during the year.

The number one activity of the committee has been that of sponsoring two research problems: one, an extensive welding research program sponsored jointly with the American Welding Society; and the other, research on insulating oils and cable saturants, project 74. J. C. Balsbaugh, who has been supervising this latter project, together with A. G. Assaf, presented, at the national technical meeting in January, some results of this work in a paper entitled, "Electrical Stability of Electrical Insulating Oils Under Limited Oxidation." The welding research program is very large, involving many problems, the total of the Welding Research Council budget being about $200,000. To this the Engineering Foundation and the Institute make rather small money contributions, but they cooperate as requested and thus keep in close touch with the work being done. The activity of the American Welding Society makes careful supervision of the project on the part of the Institute unnecessary.

Two AIEE members, Comfort A. Adams and Herman Halperin, are entitled to the thanks of the committee and the Institute for the work they have done in promoting the work of the welding research and the cable saturants research, respectively.

The committee has kept in close touch with all of the technical committees of the Institute by inviting each of those committees to designate one of its members as a special research-committee contact member. Through this means, any committee is readily able to call upon the committee on research as needed.

COMMITTEE ON SAFETY

The committee has continued its activities on the problems of safety from electrical fire and accident hazards. An important contribution was the completion of a bibliography of the publications in this field during the years of 1930 to 1942 by the Institute at the request of the committee, and the sale has been very gratifying, showing a widespread interest in information of this kind.

The effort to persuade the Student Branches to hold meetings on safety has been continued during the current school year. In March 1943, a post-card questionnaire returned by the Student Branches indicated that more than half of them held one or more meetings during the year on various phases of electrical safety. For the coming year, each Student Branch will be urged to hold a meeting on electrical-fire safety some time during the National Fire-Prevention Week in October, and also to hold at least one meeting on electrical-accident safety problems during the spring of 1944. The committee believes that safety is an excellent subject for Student Branch discussions.

Encouragement has been given to the presentation of papers and publication of articles in *Electrical Engineering* and elsewhere. One technical paper on "The Effect of Frequency of Let-Go Currents," a conference paper on the teaching of safety in one of the engineering colleges, and an article in *Electrical Engineering* on the role of lighting in accident prevention, as well as several short news items in *Electrical Engineering*, have been presented or published during the year.

The Institute has supported the activities of the National Fire Waste Council and the National Fire Protection Association, and the AIEE representatives have participated actively in the work of various committees in the American Standards Association dealing with codes covering electrical-safety problems.

War conditions make it increasingly important that electrical installations be maintained in good order, and the committee is lending its support in this direction.

Standards

STANDARDS COMMITTEE

The year 1942–43 from the standards committee's viewpoint was marked by the completion of an unusual number of standards projects, reports on proposed standards, the initiation of new lines of various wartime endeavors on the part of the many committees and subcommittees engaged in the normal processes of standards work, and also by the standards committee taking an active leading role in the promulgation of the Institute's wartime policies as defined by the committee on planning and co-ordination and adopted by the board of directors on August 7, 1942.

The war emergency has focused attention on the importance of the nationwide general use of industry standards and also on the very definite advantage to be gained through the formulation by established industry standardizing bodies of all war emergency standards or application guides for "duration" use. After careful consideration of the many technical problems involved in the preparation and use of emergency standards, the standards committee decided that in a great majority of the cases the wartime situation could be taken care of effectively by the preparation of wartime guides and interim reports covering the application of various types of standard electrical apparatus. Consequently, the standards committee, in collaboration with the technical program committee, and the special committee on co-operation with war agencies appointed by President Osborne, recommended that the Institute's technical committees prepare guides and reports covering application and operation of apparatus and, where necessary, emergency substitutions or modifications of present standards.

The preparation of some 13 or 14 such guides is now well along toward completion, and some are already completed. These guides and interim reports are educational in character and in no way mandatory. They have been prepared by technical committees for the purpose of making essential information immediately available to war industries, thus furthering the conservation of critical material and labor for the war emergency. It is expected that the standards committee will be able to publish these in suitable form for general use following the national technical meeting in Cleveland.

One instance of the immediate need for a modification of an existing standard has developed. It will be a wartime "Modification of and Supplement to" AIEE Standard 45, "Recommended Practice for Electrical Installations on Shipboard." This modification of Standard 45, sponsored by the committee on marine transportation, is to apply during the national emergency only. It is now in course of publication, and a very heavy demand from the marine field is already indicated.

It has been evident for a number of years that the growing complexity of standardizing procedure has made it ever more difficult to understand for those not continually in touch with the situation. With this problem in mind, the standards committee has developed and published a "Standards Manual." The groundwork for the manual was laid by the late R. E. Hellmund, and it is proving to be of great assistance to the many committeemen engaged in standards work. A recent development, a proposed addition to the manual, is a chart showing graphically the various steps through which standards projects must pass before final approval as AIEE and American Standards.

Among the many new and revised standards and reports considered and approved during the current year are the following: dielectric test voltage measurements; apparatus bushings; air switches and bus supports; switchgear assemblies; neutral grounding devices; wet tests; power circuit breakers; principles for selection of reference values for electrical standards; insulator tests; lightning arresters.

Several special committees and individual representatives were appointed to handle these new projects: terminal potheads; roof bushings and floor and wall bushings; general service conditions and altitude correction factors; co-ordination of AIEE and ASME test codes.

A considerable number of standards which are being handled by ASA sectional committees, on which the Institute has representatives, or for which the AIEE is acting as sponsor or joint sponsor have also been or shortly will be approved formally as American Standards. These cover the following subjects: rotating electrical machinery; transformers, induction regulators and reactors; graphical symbols for telephone, telegraph and radio use; graphical symbols for electric power control and measurement; graphical electrical symbols for architectural plans; a-c power circuit breakers, and rotating electrical machinery on railway locomotives and rail cars, and others.

It has also been necessary, because of the many personnel shifts due to the war emergency and to reorganization of inactive projects, to select a considerable number of new AIEE representatives to serve on sectional committees of ASA. Two such projects of particular interest at the present time are mercury-arc rectifiers and electric fences.

With a view toward conserving members' time and travel, only three committee meetings were held this year, and as much work as possible was handled effectively by correspondence.

UNITED STATES NATIONAL COMMITTEE OF THE INTERNATIONAL ELECTROTECHNICAL COMMISSION

The work of the International Electrotechnical Commission has been practically suspended because of the international situation. The central office is being maintained, however, and continues to receive mail at its old address in London, although its valuable records have been moved to a place of safety "for the duration." The United States National Committee of the IEC also is keeping its organization intact and will continue to meet annually so that, as soon as circumstances permit, it will be possible to resume activities.

The USNC at its annual meeting in December 1942 re-elected the following officers: E. C. Crittenden, president; L. F. Adams, vice-president; and H. S. Osborne, vice-president and treasurer.

During the year, the committee suffered the loss through death of R. E. Hellmund, and Clayton H. Sharp, its honorary president. Doctor Sharp was a founding member of the USNC and served as secretary from 1920 to 1924, as president from 1924 to 1940, and as honorary president from 1940 until his death. His absence will be particularly felt when peace returns and negotiations looking to the re-establishment of IEC work are undertaken.

Technical Committees

COMMITTEE ON AIR TRANSPORTATION

Because all members of the committee are concerned primarily with the war effort, the committee work was restricted to that which was absolutely necessary. Only one meeting was called, and that was held at Institute headquarters, January 28, 1943. All other work of the committee has been handled by correspondence.

The activities of the committee can be divided into three groups: standards, technical papers, and formation of sectional groups.

A standard is being prepared by J. W. Allen of the Bureau of Aeronautics to show the relation of temperature to altitude, knowledge of which is necessary for aviation practice. This information should be of great value to electrical engineers throughout the industry, as the extreme range in both temperature and altitude is not appreciated by most design engineers. A subcommittee is preparing revisions for all Institute standards regarding rotating machinery that may be used in aircraft. The work of this committee will deal primarily with d-c apparatus and with increasing altitude and temperature variations to care for aviation equipment.

The committee has invited and received confirmation of approximately 60 technical articles which are being prepared by electrical engineers who are active in the aviation industry. These papers have been correlated carefully into a program which will show all aspects of aviation activities. As these papers are published, it is anticipated that there will be a large influx of aviation electrical engineers into the Institute.

For the third activity of the committee, the formation of aviation divisions within local Sections, the logical starting point was the Los Angeles Section, where many aircraft plants are concentrated. More than 300 prospective members appeared for the first meeting of the proposed division. This division is now well organized and plans to meet at least six times a year to hear technical papers from its own members.

As a result of the success of the division at Los Angeles, invitations were prepared for the formation of similar divisions in other localities. At this writing, the Philadelphia Section is planning to form such a division, as eight large plants in its area employing many electrical engineers are concerned solely with aviation activities. There are many other plants in this area which have some aviation electrical activities. It is anticipated that a very active division will result. It is expected that similar divisions will be formed in the Washington–Baltimore area, the New York Section, the San Diego Section, and the Seattle Section.

COMMITTEE ON AUTOMATIC STATIONS

The committee held three meetings during the year, one at the summer convention in Chicago in June 1942, one at the national technical meeting in New York in January 1943, and one in April 1943, at which the work of the committee and subcommittees was discussed.

The bibliography on automatic stations, known as Special Publication 5, covering the period 1932–41 inclusive, was published in December 1942, and is now available at headquarters.

The subcommittee which distributed copies of a questionnaire throughout the United States and Canada, on a-c automatic-reclosing equipment applying to stub feeders, has now completed its report, which has been submitted for presentation at the national technical meeting in Cleveland in June.

A report on conductors used for supervisory control and telemetering has been prepared in connection with a questionnaire issued jointly with the committee on protective devices, and has been submitted by the committee for presentation at the national technical meeting in Cleveland in June 1943. This report covers only the data coming within the scope of the committee on automatic stations. The data prepared by the committee on protective devices was presented by that committee at the national technical meeting in January 1943.

The committee sponsored a paper on multichannel carrier-current facilities for a power line, together with a paper on automatic tie-line load control, an application to the Northwestern Electric Company's system.

A paper on a new control system for automatic parallel operation of load-ratio-control transformers was recommended by the committee on electrical machinery and the committee on automatic stations for presentation at the North Eastern District technical meeting.

A paper on experience with oil circuit reclosers on Rural Electrification Administration systems was recommended by the ommittee on protective devices and the committee on automatic stations for presentation at the national technical meeting in Cleveland.

COMMITTEE ON COMMUNICATION

The committee's activities have been quite adversely affected by the war effort, because

1. Executives and workers in the communication field have been overburdened with responsibilities.
2. Clearances for prepared papers dealing with this subject matter have been for the most part impossible to secure, particularly on up-to-date subjects.

Material, therefore, which the committee could develop and sponsor for technical meetings and for publication, has been very limited in quantity. In view of these circumstances, encouragement has been successfully given to more of the "informal-conference" method of disseminating information, and it is believed that further conferences should be encouraged for the future.

The committee has deferred for the duration the preparation of a report on the progress of the art in communications.

COMMITTEE ON DOMESTIC AND COMMERCIAL APPLICATIONS

The membership of the committee was expanded for the administrative year which began August 1. This move added new talent and diversified personal interests, and gave wide geographical representation.

At the national technical meeting in January, one conference organized by the committee was devoted to the subject

"Analysis of Military Loads and Postwar Applications." This conference placed emphasis on the need for accurate measurements of military loads, with applications and supply systems "cut to fit."

Inasmuch as wartime restrictions and conversion of industry during the year practically eliminated the production of domestic and commercial utilization devices, little or no justification was found for the committee to undertake work on applications for civilian needs. Agricultural electrification received a greater share of attention.

COMMITTEE ON ELECTRICAL MACHINERY

Committee Meetings. The committee held two meetings during the year, one at the summer convention in Chicago, and one at the national technical meeting in New York in January 1943, at which technical sessions at national and District technical meetings were discussed, and the work of the committee and its subcommittees was reviewed.

Subcommittee Activities. The synchronous machinery subcommittee has completed its work on the revision of the test code on synchronous machinery, and this code is now being reviewed by the test code coordinating subcommittee.

The transformer and induction-regulator subcommittee completed the "Interim Report on Guides for Overloading Transformers and Voltage Regulators," which was presented at the summer convention in Chicago in June 1942, and at the Pacific Coast convention in Vancouver in September 1942. The subcommittee has prepared an "Interim Report on Overloading of Distribution Transformers" which is to be presented at the national technical meeting in Cleveland. As a result of the work of the subcommittee, several papers on the loading of transformers during the war emergency were presented at the national technical meeting in New York in January 1943, and additional papers will be presented at the national technical meeting in Cleveland.

The induction machinery subcommittee, in co-operation with the committee on industrial power applications, prepared a report on "Proposed Guide for the Selection of Electric Motors and Motor Controllers," as occasioned by the war emergency, and this report was presented at the national technical meeting in New York, in January.

The insulation resistance subcommittee is working on a test code for measuring insulation resistance, and it is estimated that at least another year will be required for completion of this work.

AIEE Technical Meetings. The committee on electrical machinery sponsored one session at the summer convention, at which five papers on overload operation of transformers and rotating machinery were presented. The committee participated in joint sponsorship of a session on mercury-arc-rectifier applications at this convention.

The committee sponsored one technical session and one conference session at the national technical meeting in New York.

The committee plans to sponsor two sessions at the national technical meeting, in June, one of these to be on synchronous machinery, and the other on transformer loading.

Co-operation With Other Committees. J. F. Calvert has been designated as contact man with the committee on research. The committee on electrical machinery is represented on the subcommittee on temperature measurements of the committee on instruments and measurements and on the joint subcommittee of AIEE standards committee on outdoor and indoor cable terminal potheads, roof bushings, and floor and wall bushings.

COMMITTEE ON ELECTRONICS

By action of the board of directors, a technical committee on electronics has been set up. This committee has taken over the functions of the former joint subcommittee on electronics which has been carrying on the electronics activities of the Institute for several years. The scope of the new committee is as follows:

> Treatment of all matters in which the dominant factor is electronics, particularly those dealing with the design, characteristics, or behavior of devices of an electronic nature, or of the circuits associated with them. The treatment of complete equipments or systems may be considered within the scope of this committee if they depend predominantly on electronic devices for their operation. If the field of application of such equipments or systems falls also within the scope of other technical committees, action with those committees may be joint.

In accordance with this statement of scope, the mercury-arc-rectifier subcommittee has been transferred from the committee on electrical machinery to the committee on electronics.

Much of the work being done in electronics is closely connected with military applications, so that very little material is available for publication at this time. The program activities of the committee have been principally concerned with the organization of informal technical conferences on topics of interest in connection with the war effort.

At a meeting of the committee held during the national technical meeting in January, the question of a standardization program in electronics was discussed. In co-operation with the subcommittee on electronic standards of standards co-ordinating committee 7, a joint subcommittee on standardization policy was appointed to study and recommend such a program. This committee met and made its report. It recommended the transfer of the subcommittee on electronic devices from coordinating committee 7 to the technical committee on electronics, in order to centralize all standardization activity in the technical committee. This transfer was made, after approval of the move was given by the standards committee. The committee now operates as the subcommittee on electronic standards. The policy committee further recommended that the following program of standardization be undertaken by the committee on electronics:

1. The preparation of standards of nomenclature and definitions in its field.
2. The preparation of a basic guiding **standard** in electronics modeled on AIEE Standard 1, **"General** Principles Upon Which Temperature **Limits Are** Based in the Rating of Electrical Machinery **and** Apparatus."
3. The preparation of standard methods of measurement of the characteristics and quantities associated with electron tubes.

The first of these projects is being undertaken immediately by the subcommittee on electronic standards. The latter two should be undertaken as soon as personnel can be made available, but it is doubtful if this will be possible during the war period.

COMMITTEE ON INSTRUMENTS AND MEASUREMENTS

The pressure of war work throughout the year has materially decreased the time available for either the preparation of papers or the development of standards. Nevertheless two technical sessions on instruments and measurements were held during the year. At the meeting in Chicago, three formal technical papers were presented. Following this, a conference was held on the measurement and specifications of surface finish. At the national technical meeting in New York, five formal papers were presented at the measurements session, and two other measurement papers were given at the switching equipment session. The former group were, for the most part, related to radio communication.

At the fall meeting of the committee in November 1942, the possibility of preparing a guide or interim report covering the use of instruments under emergency conditions was considered. It was felt, however, that in the nature of things there was very little to be said except for a few exceedingly obvious procedures, and hence it was decided that no guides were required.

Two new subcommittees have been formed. The first, on measurements for industrial control, has surveyed the field, developed contacts with other societies interested in this field, and is ready to join in attacking any measurement problems which may develop in it. The other, on high-frequency measurements, is planning a number of conferences or sessions at which a knowledge of the tremendous war-stimulated development of methods in this field can be disseminated to the Institute membership. Military need for secrecy will bar many items, but it is hoped that some features may be presented even before the close of hostilities.

The subcommittee on definitions has under discussion some 26 proposed revisions and additions to the definitions in the measurements section of the American Standard, "Definitions of Electrical Terms." These changes will bring about closer agreement with the wordings used in other standards.

The subcommittee on temperature measurements has made good progress in preparing a draft of a master test code. This will soon be ready for review by the main committee. When completed, it should prove of value, both by eliminating the need for repeating the details of measurement methods in the many standards for different types of apparatus, and by guiding the art toward the use of satisfactory and generally recognized methods for temperature measurement.

The subcommittee on recording instruments has begun a revision of AIEE tentative standard 40 but finds that the art is growing very rapidly at the present time. Hence, it may be desirable to postpone the revision of this standard for a while.

COMMITTEE ON LAND TRANSPORTATION

No meeting of the committee was held in 1942, but a land-transportation program for the national technical meeting in January was developed by correspondence. This included two formal papers on selection of railroad motive power and maintenance of motive power, respectively, and two conference papers on train communication, and electrification of the Sorocabana Railway in Brazil, respectively. The latter two were presented formally, the first at the Kansas City, Mo., District technical meeting, and the second at both the Pittsfield, Mass., and the Kansas City, Mo., District technical meetings. Another paper on the advantages of high-speed traction motors was developed for the Kansas City meeting.

One session of the committee was held during the January meeting. It was decided to omit a formal session at the national technical meeting in June, but subjects for future activities were developed. These included means of developing a set of standards for grounding on electric locomotives.

The paper on selection of railroad motive power developed extensive discussion, and it has been suggested that the study be continued with the co-operation of the railroad division and the gas, oil, and power division of the American Society of Mechanical Engineers.

COMMITTEE ON MARINE TRANSPORTATION

The year 1942–43 has been a war year for the committee on marine transportation, in that Standard 45 had to be reviewed carefully, and revisions had to be made in cases where essential war materials were specified. This has necessitated considerable work on the part of each member, already burdened with war work in various shipbuilding yards and allied industries.

The War Production Board has been interested in the work of this committee, and, through the president of the Institute, arranged to have a representative present at the meetings. The purpose, as stated in their letter, was to offer suggestions for the development of war emergency standards.

The work of reviewing Standard 45 to meet existing emergency conditions has been a difficult task. A large part of the work has been done by the subcommittees on wires and cables, power applications, distribution, and fittings and appliances. The work under the cognizance of the subcommittee on wires and cables was started during the previous fiscal year, but, because of the volume of work involved, it was carried over into this year.

The first meeting to hear reports from all subcommittees was held November 6, 1942. At this time, revisions as of that date were acted upon, but not all reports were complete. The subcommittees were instructed to bring their work to a conclusion at the earliest date possible, and, by correspondence and letter ballots, their work was completed in January of this year. A meeting of the main committee was held January 22, 1943, to receive final reports and approve the revisions applicable for the duration of the present emergency. In addition to this work, some few revisions and corrections have been made to keep Standard 45 up-to-date with the best current practice for electrical installations on shipboard.

The special committee appointed last year to confer with the American Bureau of Shipping completed its work and made its report at the meeting November 6, 1942. The purpose of this special committee was to reconcile existing differences between Standard 45 and the ABS rules covering the same class of electrical requirements. The results were very satisfactory, and two widely used sets of rules are now more nearly in agreement.

An extensive report on revisions of Standard 45 was compiled and submitted to the standards committee for action at the meeting held March 25, 1943. These modifications were approved, and are solely for the purpose of conserving critical materials. They are war emergency measures to be used only for the duration. The revisions are in a form which gives the modifications to the sections altered, and will have to be used in conjunction with Standard 45. In this form, it leaves Standard 45 intact and suitable for use without reprinting upon the termination of the war and emergency conditions.

A meeting of this committee will be held before the close of the administrative year to act on any pending revisions to be issued as an addenda to the basic Standard 45. If possible, unfinished work now before the committee will be given final action in order not to carry it over into the next year.

COMMITTEE ON POWER GENERATION

Two major committee projects are in progress, one of which will be completed this year.

The standards committee, at its October 9, 1942, meeting in New York, requested that each technical committee consider the preparation of wartime guides to help the electrical industry find ways and means of getting "more out of what it has." The committee on power generation considered this and decided that, because of the many variables in the construction and operation of generating stations and systems, it could best serve the desires of industry by compiling a number of "hints" on this subject. The logical approach was to make a survey of what had been done by the manufacturers and the operating companies. Two subcommittees have completed these assignments, and a complete report entitled "Emergency Measures to Increase Output of Generating Equipment and Systems" is being prepared.

One subcommittee assembled the suggestions in connection with generators. The generator was selected, as it is one of the most important links in the station, and usually other equipment is closely associated with its design.

The other subcommittee prepared a questionnaire which was quite widely distributed to the larger operating groups. This questionnaire covered such matters as loading of generators by temperature, rebuilding, and rehabilitation; interconnected operation; effect of system voltage and frequency on load; power-factor correction; maintenance schedules; and personnel problems.

Both of these reports, in preliminary form, were presented at the national technical meeting in New York and at the District technical meeting in Kansas City. They will be presented at a joint meeting of the electrical equipment committee and prime movers committee of the Edison Electric Institute to be held in Chicago in May. The comments and suggestions received from these conferences will be incorporated in a final report that will be presented at the national technical meeting in Cleveland.

The other project, which is fast nearing completion, is the preparation of specifications for prime-mover governing. This is a joint AIEE–ASME activity and is being carried on by a special committee. The specifications will be presented at the national technical meeting in Cleveland.

COMMITTEE ON POWER TRANSMISSION AND DISTRIBUTION

On account of the war, the committee activity during the current year has followed a somewhat different pattern from that of the past. Committee meetings have been limited in number, and most activities have been carried on mainly by correspondence.

The distribution subcommittee sponsored a technical session on "Distribution Systems in Wartime" at the 1942 winter convention. By this early action, less than two months after the Pearl Harbor attack, it accomplished, in its field, a rather complete discussion of the problems confronting the electric-power industry as a result of the war situation. As no further problems of pressing need have arisen in the distribution field during the year, this subcommittee's activities have been confined largely to sponsoring papers within its field.

The general systems subcommittee has been active during the past year in preparing a comprehensive report on "Shunt Capacitors," which was presented at a technical session at the national technical meeting in January 1943.

The transmission subcommittee sponsored two conferences at the January meeting, one on "Emergency Loading of Transmission Systems," at which six conference papers were presented and discussed, and the other on "Emergency Rating of Power Cables," which likewise included six conference papers. Steps are now being taken to have these last six conference papers compiled under one cover and made available in pamphlet form.

This subcommittee is also planning to

sponsor, at the national technical meeting in June, other papers or committee reports in the same field touching on emergency overloading of transmission conductors and cables.

The lightning and insulator working group of the transmission subcommittee completed the proposed revision of Standard 41, known as 41-*A* in revised form, covering the subject of "Insulator Tests." This revision was approved by the main committee and forwarded to the standards committee with recommendations for its adoption as a standard. It is understood that the standards committee will offer this standard to ASA with the recommendation that appropriate action be taken for it to appear as an American Standard.

The stations subcommittee sponsored a conference at the January meeting entitled "Substation Designs to Meet Wartime Conditions." Thirteen contributors presented material on a wide range of subjects including protective lighting, antisabotage measures, minimum use of critical materials, substitution of noncritical materials, and operating methods to increase the available electric capacity.

In addition to the conferences just referred to, which were organized and made possible by the active work of the subcommittees, the main committee sponsored three technical sessions at the January meeting: one on capacitor generators and transformers, the second on transmission lines, and the third on transmission and distribution. A large majority of the papers in these technical sessions dealt, directly or indirectly, with problems relating to aiding the war effort in the electrical industry.

The committee is planning two or more technical sessions at the meeting in June.

In these wartimes, it seems undesirable to hold frequent committee meetings except where major problems of importance arise which affect the war effort. There are several new projects which have been suggested for consideration by the committee, but it has not been considered expedient under existing conditions to initiate active work on them at this time. The starting of new projects in the main committee, its subcommittees, or working groups, unless of vital importance, is believed to be more of a detriment to the war effort than an aid, and it is planned for the future to limit committee activities, so far as feasible, to the consideration and development of projects essential to a successful prosecution of the war.

COMMITTEE ON PROTECTIVE DEVICES

The war has had marked effects upon the work of the committee. Several peacetime activities have been dropped. It has been necessary to develop as rapidly as possible two wartime guides for the conservation of materials in construction. Certain standards have been pushed to completion as rapidly as possible. Three standards have been approved by the board of directors, and another has received the approval of the standards committee.

The committee sponsored 36 technical papers presented at national meetings and two presented at the District meeting in Pittsfield. It also sponsored five conference papers.

The major divisions of the work of the committee were assigned to four subcommittees: circuit breakers, switches, and fuses; fault-current-limiting devices; lightning arresters; and relays.

Circuit Breakers, Switches, and Fuses. The subcommittee on circuit breakers, switches, and fuses prepared the "Interim Report on Application and Operation of Circuit Breakers and Switchgear," which, in preliminary draft form, was discussed at the national technical meeting in January, and in final form at the North Eastern District technical meeting in Pittsfield.

Standard 19 on oil circuit breakers has been revised, and a working group has been organized to revise Standard 20 on air circuit breakers. It is expected that the revision of proposed Standard 25 on fuses will be completed soon.

Fault-Current-Limiting Devices. A subcommittee report, "Correlation of System Overvoltages and System Grounding Impedance," was presented at the January meeting. A preliminary form of questionnaire on practices in grounding power systems has been prepared, and the final form is expected to be completed soon. Comments and criticisms on Standard 32, on neutral grounding devices, are being collected.

Lightning Arresters. An operating guide for the use of lightning arresters was presented as an individual paper at the January meeting, and is scheduled for presentation as a subcommittee report at the national technical meeting in June.

Standard 24 on protector tubes has been published in report form. Work on a report on distribution tubes is in progress, and it may be combined with the former.

Standard 28 on lightning arresters has been approved by the Institute and transmitted to ASA for consideration.

Relays. The subcommittee on relays has been actively engaged in a number of studies: pilot-wire experience, out-of-step protection, manufacturers' catalog information, relay-testing-procedure questionnaire, and a report on experience with relay systems under actual combat conditions.

Section 4 of American Standard *C*-57 on instrument transformers has been revised, and comments are being studied for final revision following a year's trial use. Standard 31 on potential devices and coupling capacitors has completed its period of trial use.

A conference on relay testing and wartime relay problems was held at the January meeting.

Awards

COMMITTEE ON AWARD OF INSTITUTE PRIZES

Four national prizes and ten District prizes were awarded for papers presented during the calendar year 1941 and for the student papers presented during the academic year ending June 30, 1942.

The papers considered were of a high order, and in determining the awards for the national best paper prizes and the initial paper prize the committee had the benefit of gradings and recommendations of the technical committees. In addition to the four papers which were awarded national prizes, honorable mention was given to others.

The changes made in the rules on award of national and District prizes, issued in the new edition of January 1941, have been in effect for more than two years and have been found to be working satisfactorily.

EDISON MEDAL

The Edison Medal, which is awarded by a committee composed of 24 members of the Institute, was awarded for 1942 to Edwin H. Armstrong, "for distinguished contributions to the art of electric communication, notably the regenerative circuit, the superheterodyne, and frequency modulation," and was presented January 27, 1943, during the national technical meeting. The medal may be awarded annually for "meritorious achievement in electrical science, electrical engineering, or the electrical arts."

LAMME MEDAL

The Lamme Medal committee awarded the medal for 1942 to Joseph Slepian, associate director of research, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., "for his contributions to the development of circuit-interrupting and current-rectifying apparatus." Arrangements are being made for the presentation of the medal at the national technical meeting in Cleveland, Ohio, June 21–25, 1943. The medal may be awarded annually to a member of the AIEE "who has shown meritorious achievement in the development of electrical apparatus or machinery."

JOHN FRITZ MEDAL

The John Fritz Medal board of award, composed of representatives of the American Society of Civil Engineers, American Institute of Mining and Metallurgical Engineers, American Society of Mechanical Engineers, and AIEE, awarded the 39th medal for 1943, to Willis R. Whitney, vice-president in general charge of research, General Electric Company, Schenectady, N. Y., "for distinguished research, both as an individual investigator and as an outstanding and inspiring administrator of pioneering enterprise, co-ordinating pure science with the service of society through industry." It was presented January 27, during the AIEE meeting.

HOOVER MEDAL

The Hoover Medal was established through a trust fund created by a gift from Conrad N. Lauer, and is to be awarded periodically "to a fellow engineer for distinguished public service" by a board representing the national societies of civil, mining and metallurgical, mechanical, and electrical engineers. The sixth medal, for 1942, was awarded to Gerard Swope, president, General Electric Company, and was presented January 27, during the AIEE meeting.

ALFRED NOBLE PRIZE

This prize, established in 1929, consists of a certificate and a cash award from the income of a fund contributed by engineers and others to perpetuate the name and achievements of Alfred Noble, past president of ASCE, and of the Western Society of Engineers. It may be made to a member of any of the co-operating societies, ASCE, AIME, ASME, AIEE, or WSE, for a technical paper of particular merit accepted by the publication committee of any of these societies, provided the author, at the time of such acceptance, is not over 30 years of age. The award for 1941–42 was presented to George Wesley Dunlap (A'35, M'42), General Electric Company, Schenectady, N. Y., for his paper "The Recovery Voltage Analyzer for Determination of Circuit Recovery Characteristics," published in AIEE *Transactions*, volume 60, 1941, November section, pages 958–62.

WASHINGTON AWARD

Audrey A. Potter, dean of engineering, Purdue University, Lafayette, Ind., and chairman of the National Advisory Committee on Engineering and War Training, was awarded the Washington Award for 1943 for "distinguished leadership in engineering education and research, and patriotic service in mobilizing technical knowledge for victory in war and peace." The presentation was made at a dinner on February 24, 1943. This award may be made annually to an engineer, by the commission of award composed of nine representatives of the Western Society of Engineers, and two each of ASCE, AIME, ASME, and AIEE.

THE MARSTON MEDAL

The Marston Medal was established in 1938 by the Iowa State College through the inspiration and generosity of Anson Marston, dean emeritus of engineering. It is awarded, not oftener than once a year, to an engineering alumnus of at least 30 years' standing for achievement in engineering in a broad field. The award is made by a board composed of four alumni, one representative each of the ASCE, ASME, AIEE, and American Institute of Chemical Engineers, and the dean of engineering who serves as chairman. The presentation is a feature of the commencement exercises.

Awards have been made as follows:

1938	E. O. Shreve
1939	Thomas H. MacDonald
1940	James W. Hook

CHARGES LE GEYT FORTESCUE FELLOWSHIP COMMITTEE

Because of conditions brought about by the war there was no meeting of the committee and no award made for the year 1943–44. This action is the same as was taken last year for similar reasons.

Joint Activities

UNITED ENGINEERING TRUSTEES, INC.

This organization manages property and funds held jointly by the four founder societies, including the Engineering Societies Building, the Engineering Societies Library, and the endowment funds of the Engineering Foundation.

During the year, a considerable program of renovation of the Engineering Societies Building was completed, despite increases in cost and difficulties in securing materials. Labor and material costs have increased, income from rental of halls and auditorium has been reduced, and it is expected that these adverse effects will become more pronounced.

The building is reported to be in excellent condition and is fully occupied by the Founder Societies and associates.

An abstract of the annual report of the corporation for the year which ended September 30, 1942, appeared in *Electrical Engineering* in January 1943, pages 43–4.

ENGINEERING FOUNDATION

The Engineering Foundation was established in 1914 as a joint organization of the four societies, ASCE, AIME, ASME, and AIEE, for "the furtherance of research in science and engineering, and the advancement in any other manner of the profession of engineering and the good of mankind." Each research project supported by it is under the sponsorship of one of the founder societies.

In recent years, the Institute has been sponsor for three research projects: stability of impregnated-paper insulation, at the Johns Hopkins University; insulating oils and cable saturants, at the Massachusetts Institute of Technology; and the comprehensive program of the Welding Research Council, with the American Welding Society as joint sponsor. The last two are still receiving Engineering Foundation support.

During its fiscal year 1942–43, the Engineering Foundation is supplying funds for 17 research projects bearing on war work and also is supporting studies in the development of the engineer through its continuing contribution to the Engineers' Council for Professional Development.

A comprehensive abstract of the annual report for the year which ended September 30, 1942, appeared in *Electrical Engineering* in January 1943, pages 44–6.

ENGINEERING SOCIETIES LIBRARY

The Engineering Societies Library, which was formed by combining the separate libraries of the four national societies of civil, mining and metallurgical, mechanical, and electrical engineers, with its composite card catalog now in preparation, probably constitutes the best collection of its type in the United States. On September 30, 1942, the library had 154,770 volumes.

Special services rendered by the library include: photoprints, searches, abstracts, translations, bibliographies, book loans by mail, and others. The library board has been considering means by which members might be informed more adequately regarding the services available.

An abstract of the annual report of the library appeared on pages 46–7 of *Electrical Engineering*, January 1943.

EMPLOYMENT SERVICE

Operating as an incorporated nonprofit-making organization, the Engineering Societies Personnel Service, Inc., assists members of the founder societies desiring to secure new positions and conversely assists employers searching for engineers. Nonmembers may receive such assistance when positions available cannot be filled by members.

Offices are operated in New York, N. Y., Boston, Mass., Detroit, Mich., Chicago, Ill., and San Francisco, Calif., with the co-operation of the Engineering Societies of New England in Boston, the Engineering Society of Detroit in that city, the Western Society of Engineers in Chicago, and the Engineers Club of San Francisco in that city.

The service is supported by the joint contributions of the societies and the individuals who secure positions. An analysis of registration and placement records as reported to the national societies is given in Table XI.

ENGINEERS' COUNCIL FOR PROFESSIONAL DEVELOPMENT

This Council, organized in 1932 to engage in activities leading toward the en-

Table XI. Analysis of Employment Service

	Men Registered						Men Placed					
Month	New York	Chicago	San Francisco	Detroit	Boston	Total	New York	Chicago	San Francisco	Detroit	Boston	Total
1942												
May	159	43	71	32		265	42	20	32	8		102
June	119	60	54	36	21	290	38	22	32	11	3	106
July	100	55	51	24	64	294	40	18	31	11	4	104
August	132	47	48	36	41	304	40	13	25	17	5	100
September	107	49	53	29	42	280	31	15	27	9	11	93
October	100	35	34	26	33	228	31	13	17	10	14	85
November	91	28	43	14	43	219	30	14	15	6	20	85
December	84	27	39	15	38	203	31	12	25	5	11	84
1943												
January	123	22	52	25	29	251	38	14	27	6	3	88
February	120	44	56	28	36	284	20	11	18	7	8	64
March	130	42	57	23	22	274	30	15	13	8	9	75
April	112	26	51	17	29	235	30	12	14	7	10	73
Total	1,377	478	609	305	398	3,127	401	179	276	105	98	1,059

hancement of the professional status of the engineer, includes three representatives of each of the eight participating organizations, which are the national societies of civil, electrical, mechanical, and mining and metallurgical engineers, the Society for the Promotion of Engineering Education, the National Council of State Boards of Engineering Examiners, and the Engineering Institute of Canada. Its principal activities have been carried on by four committees: student selection and guidance, engineering schools, professional training, and professional recognition.

The status of the accrediting program, after actions taken at the annual meeting on October 18, 1942, and not including options under accredited curricular, is given below:

Curricula	
Fully accredited	496
Provisionally accredited	75
Not accredited	101
Action pending or deferred	5
	677

War problems and their effects upon the work of ECPD were thoroughly discussed at the annual meeting October 18, plans were developed for carrying out ECPD functions locally, a statement of canons of ethics for engineers was approved in principle and submitted to the constituent societies, a new special committee on unionization of engineers was authorized, and the publication of a "Manual for Junior Engineers" was authorized.

A comprehensive report on the annual meeting, including the reports of committees was published in *Electrical Engineering* in December 1942, pages 626–30.

JOINT CONFERENCE COMMITTEE

The joint conference committee, composed of the presidents and secretaries of the founder societies and the American Institute of Chemical Engineers, met in New York April 19, 1943, and considered numerous topics of interest to the several societies.

REPRESENTATIVES

In addition to the representatives in the joint organizations mentioned above, the Institute is represented in numerous other joint activities of interest to engineers in general. The names of its representatives upon nearly 30 joint committees and national organizations were published in the September 1942 issue of *Electrical Engineering*, pages 485–6, and in the 1943 Year Book.

Appreciation

With particular pride in the accomplishments of the Institute during the past year, despite the prevailing difficulties, the board of directors thanks the national committees, and the District, Section, and Branch officers for their continuing enthusiasm and their generous contributions of time and effort; it thanks the members in general for their keen interest in and support of all Institute activities.

Respectfully submitted for the board of directors.

H. H. HENLINE
May 27, 1943 *National Secretary*

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Balance Sheet April 30, 1943

Exhibit A

ASSETS		
Property Fund Assets:		
One-fourth interest in physical properties of United Engineering Trustees, Inc.:		
Land, buildings, and equipment (less depreciation and renewal reserve)	$389,888.61	
Funded depreciation and renewal reserve	108,559.87	
Total	$498,448.48	
Equipment:		
Library—volumes and fixtures—estimated value	36,366.37	
Office furniture and fixtures (less reserve for depreciation, $26,287.30)	8,630.42	
Works of art, etc.	3,001.35	
Investment less reserve in full $943.73—Schedule 1		
Total property fund assets		$546,446.62
Restricted Fund Assets:		
Securities—at cost, less reserve, $20,779.92 (quoted market value, $275,195.03) Schedule 1	$278,398.33	
Cash (Exhibit C)	49,390.36	
Accrued interest receivable	462.62	
Total restricted fund assets		328,251.31
Operating Fund Assets:		
Cash (Exhibit B)	$ 54,204.19	
Accounts receivable:		
Members—for dues (less reserve, $8,000.00)	10,075.64	
Advertisers	516.70	
Miscellaneous	1,931.88	
Accrued interest receivable	3,594.63	
Inventories:		
Transactions, etc.	1,468.50	
Electrical Definitions	6,792.50	
Work in process (May issue of *Electrical Engineering*, etc.)	4,909.63	
Text and cover paper	7,106.75	
Badges	1,697.49	
Total operating fund assets		92,297.91
Total		$966,995.84

LIABILITIES		
Property Fund Reserve		$546,446.62
Restricted Fund Reserves:		
Reserve Capital fund	$281,156.04	
Pension fund	28,187.50	
Life Membership fund	7,761.92	
International Electrical Congress of St. Louis Library fund	5,488.05	
Lamme Medal fund	4,569.83	
Mailloux fund	1,087.97	
Total restricted fund reserves		328,251.31
Operating Fund Reserves, Liabilities, Etc.:		
Accounts payable	$ 13,485.63	
Deferred income:		
Dues received in advance	4,877.07	
Entrance fees and dues advanced by applicants for memberships	1,199.60	
Miscellaneous (including unallocated receipts)	1,070.51	
Subscriptions to publications received in advance	5,450.82	
Operating fund reserves	66,214.28	
Total operating fund reserves, liabilities, etc.		92,297.91
Total		$966,995.84

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Securities Owned, April 30, 1943

Schedule 1

	Principal Amount of Bonds or Number of Shares of Stock	Reserve Capital Fund	Restricted Funds					Property Fund (Equipment Replacements)
			Pension Fund	Life Membership Fund	International Electrical Congress Library Fund	Lamme Medal Fund	Total Restricted Fund	
Railroad Bonds:								
Allegheny Corporation 20 year collateral trust conv. 5%, due 1949 (stamped)	$15,000.00	$ 10,627.50					$ 10,627.50	
Baltimore & Ohio Railroad Company 6% refunding and general mortgage series C, due 1995 (stamped)	12,000.00	8,940.00				$4,330.00	13,270.00	
Chicago & Erie Railroad Company 5% first mortgage, due 1982	1,000.00	1,105.00					1,105.00	
New York Central Railroad Company 5% refunding and improvement mortgage series C, due 2013	6,000.00	5,742.50					5,742.50	
Northern Pacific Railway Company 6% refunding and improvement mortgage series B, due 2047	10,000.00	10,962.50					10,962.50	
St. Louis-San Francisco Railway Company 5% prior lien mortgage series B, due 1950 (certificates of deposit)	6,000.00*	5,497.50*					5,497.50*	
Total railroad bonds		$ 42,875.00				$4,330.00	$ 47,205.00	
Public Utility Bonds:								
New York & Queens Electric Light & Power Company 3½% first and consolidated mortgage, due 1965	10,000.00	$ 11,000.00					$ 11,000.00	
Bond and Real Estate Mortgage:								
Fidelity Union Title and Mortgage Guaranty Company first mortgage certificates (on property 75–79 Prospect Street, East Orange, N. J.) 4%, due 1944	14,132.00*	$ 13,188.27*					$ 13,188.27*	$943.73*
United States Government Bonds:								
Treasury Savings bonds series D, due July 1, 1949	10,000.00	$ 7,644.00					$ 7,644.00	
Treasury Savings bonds series D, due January 1, 1950	10,000.00	7,500.00					7,500.00	
Treasury bonds 2%, due 1950/48	77,000.00	78,130.94					78,130.94	
Treasury bonds, 2½%, due 1968/63	20,000.00	15,000.00		$5,000.00			20,000.00	
Treasury bonds 2½% due 1972/67	6,300.00	1,100.00			$5,200.00		6,300.00	
Defense bonds series F, due June 1, 1953	34,000.00	25,160.00					25,160.00	
Defense bonds series G, 2½%, due December 1, 1954	18,000.00		$18,000.00				18,000.00	
Total United States Government bonds		$134,534.94	$18,000.00	$5,000.00	$5,200.00		$162,734.94	
Capital Stocks:								
American Can Company	60 shares	$ 4,988.40					$ 4,988.40	
American Telephone & Telegraph Company	30 shares	4,897.95					4,897.95	
Boston Edison Company	200 shares	4,927.50					4,927.50	
Commonwealth Edison Company	200 shares	7,580.68					7,580.68	
Eastman Kodak Company	35 shares	4,768.23					4,768.23	
E. I. du Pont de Nemours Company	40 shares	6,398.20					6,398.20	
General Electric Company	130 shares	4,463.80					4,463.80	
General Motors Corporation	100 shares	4,235.53					4,235.53	
International Harvester Company	100 shares	5,030.50					5,030.50	
International Match Realization Co., Ltd. voting trust certificates for capital shares of International Match Corporation	6 shares*	2,094.15*					2,094.15*	
Sears Roebuck & Company	100 shares	6,014.97					6,014.97	
Standard Oil Company of New Jersey	110 shares	4,791.78					4,791.78	
Union Carbide & Carbon Corporation	70 shares	4,858.35					4,858.35	
Total capital stocks		$ 65,050.04					$ 65,050.04	
Total		$266,648.25	$18,000.00	$5,000.00	$5,200.00	$4,330.00	$299,178.25	$943.73
*Less reserve in full for the securities designated *, considered to be of doubtful value		$ 20,779.92					$ 20,779.92	$943.73
Total Securities, Less Reserve		$245,868.33	$18,000.00	$5,000.00	$5,200.00	$4,330.00	$278,398.33	

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Statement of Cash Receipts and Disbursements of Operating Fund for the Year Ended April 30, 1943

Exhibit B

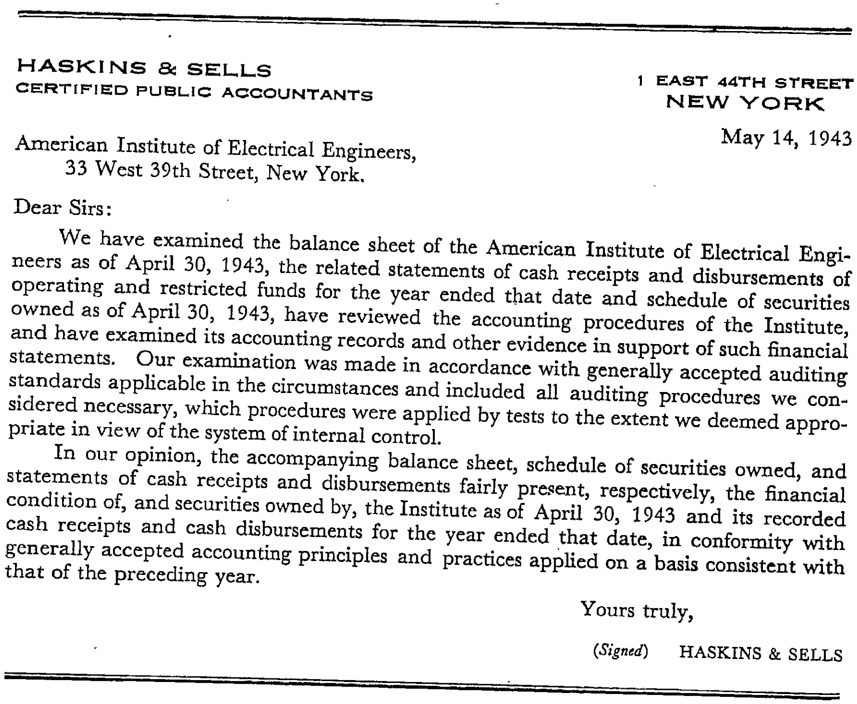
Cash on Deposit With The National City Bank of New York, May 1, 1942		$ 16,633.48
Receipts:		
Dues (including $99,684.00 allocated to *Electrical Engineering* subscriptions)	$222,544.15	
Advertising	85,586.44	
Transactions subscriptions	4,802.03	
Electrical Engineering subscriptions	10,142.38	
Electrical Definitions	13,205.05	
Miscellaneous publications (preprints, Standards, *Transactions* index, etc.)	17,020.75	
Students' fees	13,337.75	
Entrance fees	11,384.51	
Membership badges	3,585.20	
Transfer fees	1,503.37	
Interest and dividends on investments of Restricted Capital fund	6,780.28	
Transfer from Life Membership fund	1,350.12	
Miscellaneous	706.38	
Total receipts		391,948.41
Total		$408,581.89
Disbursements:		
Publication expense:		
Electrical Engineering	$ 98,867.51	
Transactions	7,540.41	
Year Book	9,884.71	
Miscellaneous publications (preprints, Standards, *Transactions* supplement, etc.)	14,119.12	
Institute meetings	14,805.96	
Institute Sections	39,416.78	
Institute Branches	3,113.79	
Edison Medal committee	186.69	
Finance committee	701.10	
Headquarters committee	50.72	
Membership committee	9,307.62	
Standards committee:		
Electrical Definitions	18,055.17	
Others	8,669.83	
Committee on cooperation with war agencies	212.35	
Technical committees	464.47	
Forward	$225,396.23	$408,581.89
Total (forward)		$408,581.89
Disbursements (forward)	$225,396.23	
Traveling expenses:		
Geographical Districts:		
Executive committees	2,768.68	
Vice-presidents	482.82	
Branch counselors and chairmen	4,254.11	
President's appropriation	1,483.73	
Board of directors	6,500.91	
National nominating committee	1,226.67	
Administrative expenses	50,061.53	
Geographical Districts—Branch paper prizes	324.00	
Institute prizes, national	409.00	
American Co-ordinating Committee on Corrosion	25.00	
American Standards Association	1,500.00	
United Engineering Trustees, Inc.:		
Building assessment	10,984.81	
Library assessment	10,401.60	
Engineering Societies Personnel Service, Inc.	814.50	
Engineers' Council for Professional Development	1,700.00	
Engineers Defense Board	110.00	
Engineering Foundation Projects:		
Insulating oils and cable saturants	250.00	
Welding research	250.00	
Hoover medal	83.25	
International Committee on Illumination	200.00	
John Fritz medal	269.75	
Production clinics	80.34	
Committee on radio broadcasts	174.30	
National Fire Protection Association—Dues	60.00	
Membership badges	3,766.30	
Legal services	250.00	
Transfers:		
To Pension fund	10,000.00	
To Reserve Capital fund	20,000.00	
Office furniture and fixtures, and repairs	530.17	
Miscellaneous	20.00	
Total disbursements		354,377. 7
Cash on Deposit With The National City Bank of New York, April 30, 1943		$ 54,204.19

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Statement of Cash Receipts and Disbursements of Restricted Funds for the Year Ended April 30, 1943

Exhibit C

	Restricted Funds						
	Reserve Capital Fund	Pension Fund	Life Membership Fund	International Electrical Congress of St. Louis Library Fund	Lamme Medal Fund	Mailloux Fund	Total Restricted Funds
Cash on Deposit With The National City Bank of New York and Various Savings Banks, May 1, 1942	$35,520.87	$18,000.00	$8,805.91	$141.80	$206.83	$1,075.85	$ 63,751.26
Receipts:							
Interest on bonds				$130.00	$ 48.00		$ 178.00
Interest on bank balances			$ 31.05			$ 12 12	43.17
Proceeds from sale, etc. of securities	$ 9,944.84						9,944.84
Transfer from operating fund	20,000.00	$10,000.00					30,000.00
Life membership fee			228.21				228.21
Total receipts	$29,944.84	$10,000.00	$ 259.26	$130.00	$ 48.00	$ 12.12	$ 40,394.22
Total	$65,465.71	$28,000.00	$9,065.17	$271.80	$254.83	$1,087.97	$104,145.48
Disbursements:							
Purchase of securities	$30,178.00	$18,000.00	$5,000.00				$ 53,178.00
Lamme gold medal, bronze replica, and certificate					$227.00		227.00
Transfer to operating fund			1,350.12				1,350.12
Total disbursements	$30,178.00	$18,000.00	$6,350.12		$227.00		$ 54,755.12
Balance on Deposit With The National City Bank of New York and Various Savings Banks, April 30, 1943	$35,287.71	$10,000.00	$2,715.05	$271.80	$ 27.83	$1,087.97	$ 49,390.36



HASKINS & SELLS
CERTIFIED PUBLIC ACCOUNTANTS

1 EAST 44TH STREET
NEW YORK

May 14, 1943

American Institute of Electrical Engineers,
33 West 39th Street, New York.

Dear Sirs:

We have examined the balance sheet of the American Institute of Electrical Engineers as of April 30, 1943, the related statements of cash receipts and disbursements of operating and restricted funds for the year ended that date and schedule of securities owned as of April 30, 1943, have reviewed the accounting procedures of the Institute, and have examined its accounting records and other evidence in support of such financial statements. Our examination was made in accordance with generally accepted auditing standards applicable in the circumstances and included all auditing procedures we considered necessary, which procedures were applied by tests to the extent we deemed appropriate in view of the system of internal control.

In our opinion, the accompanying balance sheet, schedule of securities owned, and statements of cash receipts and disbursements fairly present, respectively, the financial condition of, and securities owned by, the Institute as of April 30, 1943 and its recorded cash receipts and cash disbursements for the year ended that date, in conformity with generally accepted accounting principles and practices applied on a basis consistent with that of the preceding year.

Yours truly,

(Signed) HASKINS & SELLS

Officers and Committees for 1943–44

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(*Term expires July 31, 1944*)

Junior Past Presidents

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(*Term expires July 31, 1944*)

HAROLD S. OSBORNE	New York, N. Y.

(*Term expires July 31, 1945*)

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3	CHARLES R. JONES	New York, N. Y.
5	A. G. DEWARS	Minneapolis, Minn.
7	E. T. MAHOOD	Kansas City, Mo.
9	E. W. SCHILLING	Bozeman, Mont.

(*Terms expire July 31, 1944*)

District		
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4	C. W. RICKER	New Orleans, La.
6	L. A. BINGHAM	Lincoln, Nebr.
8	J. M. GAYLORD	Los Angeles, Calif.
10	W. J. GILSON	Toronto, Ont.

(*Terms expire July 31, 1945*)

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R. G. WARNER	New Haven, Conn.

(*Terms expire July 31, 1944*)

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T. G. LECLAIR	Chicago, Ill.
F. R. MAXWELL, JR.	Pensacola, Fla.

(*Terms expire July 31, 1945*)

K. L. HANSEN	Milwaukee, Wis.
W. B. MORTON	Philadelphia, Pa.
W. R. SMITH	Newark, N. J.

(*Terms expire July 31, 1946*)

C. M. LAFFOON	East Pittsburgh, Pa.
C. W. MIER	Dallas, Tex.
S. H. MORTENSEN	Milwaukee, Wis.

(*Terms expire July 31, 1947*)

National Treasurer

W. I. SLICHTER	New York, N. Y.

(*Term expires July 31, 1944*)

National Secretary

H. H. HENLINE	New York, N. Y.

(*Term expires July 31, 1944*)

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BRAZIL—Richard H. Bowles, Sao Paulo Tramway Light and Power Company, Sao Paulo

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FRANCE—A. S. Garfield, 173 Boulevard Haussmann, Paris, 8E

INDIA, NORTHERN—V. F. Critchley, 17 Bahawalpur Road, Lahore, Punjab

INDIA, SOUTHERN—N. N. Iengar, The Tata Power Co. Ltd., Bombay House, Bruce Street, Fort Bombay

NEW ZEALAND—P. H. Powell, Canterbury College, Christchurch

SWEDEN—A. F. Enstrom, Ingeniorsvetenskrapsakademien, Stockholm 5

TRANSVAAL, SOUTH AFRICA—W. Elsdon-Dew, P.O. Box 4563, Johannesburg

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H. S. Osborne
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M. C. Beebe
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C. W. Fick
A. L. Harding
L. F. Hickernell
Sidney Withington
A. E. Knowlton
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Alexander Maxwell
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D. D. Ewing
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(*Terms expire July 31, 1944*)
K. T. Compton W. D. Coolidge J. M. Thomson
(*Terms expire July 31, 1945*)
J. T. Barron R. E. Doherty J. V. B. Duer
(*Terms expire July 31, 1946*)
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(*Terms expire July 31, 1947*)
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(*Term expires July 31, 1948*)

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(*Terms expire July 31, 1944*)
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(*Terms expire July 31, 1945*)

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H. H. Henline, national secretary
(*Terms expire July 31, 1944*)

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W. R. Smith

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(*Terms expire July 31, 1944*)
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(*Terms expire July 31, 1945*)
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(*Terms expire July 31, 1945*)
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Allan R. Dixon (3)
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Donald Thomas (6)
C. V. Waddington (7)
Ralph A. Hopkins (8)
Richard Setterstrom (9)
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John Grotzinger
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R. L. Lloyd
A. C. Muir
Albrecht Naeter
C. N. Rakestraw
E. C. Rue
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Hugh L. Smith
Herbert Speight
Jerome J. Taylor
E. E. Turkington
L. A. Umansky
R. L. Walsh
W. E. Wickenden
R. H. Wright

Land Transportation

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P. H. Hatch, *secretary*, 514 Siwanoy Place, Pelham Manor, N. Y.

J. C. Aydelott
R. Beeuwkes
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C. M. Davis
E. B. Fitzgerald
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Fraser Jeffrey
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Paul Lebenbaum
P. A. McGee
T. H. Murphy
J. A. Noertker
F. W. Willcut

Light, Production and Application of

W. C. Kalb, *chairman*, National Carbon Co., Carbon Sales Division, Cleveland, Ohio

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A. G. Dewars
C. L. Dows
S. G. Hibben
O. W. Holden
H. E. Mahan
J. A. McDermott
P. S. Millar
G. T. Minasian
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Harris Reinhardt
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E. M. Strong
C. C. Whipple
I. A. Yost

Marine Transportation

W. N. Zippler, *chairman*, Gibbs & Cox, Inc., 1 Broadway, New York 4, N. Y.

E. C. Alger
R. A. Beekman
R. D. Bennett
H. C. Coleman
P. J. Dumont
J. B. Feder
A. R. Gatewood
L. M. Goldsmith
H. F. Harvey, Jr.
P. B. Harwood
C. J. Henschel
Alex Kennedy, Jr.
Clarence Lynn
F. R. Maxwell, Jr.
I. H. Osborne
Wm. H. Reed
E. M. Rothen
P. E. Vance
O. A. Wilde

Power Generation

A. C. Monteith, *chairman*, Westinghouse Electric & Mfg. Co., East Pittsburgh, Pa.

F. A. Annett
C. A. Corney
R. P. Crippen
John Joseph Dougherty
H. A. Dryar
J. H. Foote
A. H. Frampton
R. L. Frisby
Frazer W. Gay
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J. A. Koontz
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G. M. Pollard
Charles R. Reid
Herbert B. Reynolds
M. J. Steinberg
H. D. Taylor
Robert Treat
F. D. Troxel

Technical Committees (continued)

Instruments and Measurements

Chester L. Dawes, *chairman*, Harvard University, Pierce Hall, Cambridge, Mass.
T. S. Gray, *vice-chairman*, Massachusetts Institute of Technology, Cambridge, Mass.
E. I. Green, *secretary*, Bell Telephone Laboratories, 180 Varick St., New York 14, N. Y.

Perry A. Borden
A. L. Brownlee
L. A. Burckmyer, Jr.
C. T. Burke
D. T. Canfield
A. G. Dewars
E. D. Doyle
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Stanley Green
C. M. Hathaway
N. S. Hibshman
G. B. Hoadley
F. C. Holtz
I. F. Kinnard
W. G. Knickerbocker
A. E. Knowlton
H. C. Koenig
Everett S. Lee
J. T. Lusignan
Paul MacGahan
H. C. Otten
W. E. Pakala
G. R. Patterson
E. G. Ratz
A. R. Rutter
F. B. Silsbee
Roy M. Smith
H. M. Turner

Power Transmission and Distribution

H. E. Wulfing, *chairman*, Commonwealth Edison Co., 72 W. Adams St., Chicago, Ill.

D. L. Beeman
D. K. Blake
C. D. Brown
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F. V. Smith
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J. J. Torok
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L. T. Williams

Protective Devices

J. B. MacNeill, *chairman*, Westinghouse Electric & Mfg. Company, East Pittsburgh, Pa.

A. E. Anderson
H. D. Braley
O. E. Charlton
J. E. Clem
H. W. Collins
F. W. Cramer
William Deans
F. M. Defandorf
G. B. Dodds
W. S. Edsall
F. R. Ford
L. R. Gamble
I. W. Gross
H. W. Haberl
F. C. Hanker
E. L. Harder
R. T. Henry
P. A. Jeanne
L. F. Kennedy
H. E. Kent
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T. G. LeClair
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H. J. Lingal
F. R. Longley
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J. R. North
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F. V. Smith
Roy M. Smith
H. R. Stewart
H. E. Strang
H. R. Summerhayes
P. L. Taylor
J. J. Tesar
J. M. Towner
J. D. Wood

Therapeutics, Applications of Electricity to

C. V. Aggers, *chairman*, Westinghouse Elec. & Mfg. Co., X-Ray Div., 2519 Wilkins Ave., Baltimore 3, Md.

Lloyd L. Call
W. D. Coolidge
Roy Kegerreis
W. B. Kouwenhoven
H. C. Rentschler
C. W. Ricker

Special Committee

Registration of Engineers

C. R. Beardsley, *chairman*, New York City Department of Commerce, 60 Broadway, Room 1404, New York 4, N. Y.

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Tomlinson Fort
N. B. Hinson
T. G. LeClair
K. B. McEachron
W. H. Stueve
J. G. Wray

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Robin Beach

American Association for the Advancement of Science, Council

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I. Melville Stein

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American Coordinating Committee on Corrosion

H. S. Phelps

American Research Committee on Grounding

C. T. Sinclair

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H. S. Osborne

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E. B. Paxton

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U. S. National Committee, International Electrotechnical Commission

H. L. Huber
V. M. Montsinger
J. R. North

Alternates

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E. L. Moreland
E. B. Paxton

Washington Award Commission

H. B. Gear
L. R. Mapes

World Power Conference, Executive Committee of U. S. National Committee

Nevin E. Funk

General Counsel

Parker & Aaron
20 Exchange Place, New York 5, N. Y.

Student Branches of the Institute

Name and Location	District	Counselor (Member of Faculty)	Chairman	Secretary
Akron, Univ. of, Akron, Ohio	2	A. J. B. Fairburn	Robert Towne	Stanley Myers
Alabama Polytechnic Inst., Auburn, Ala.	4	W. W. Hill	J. T. McRae	Warner Sinback
Alabama, Univ. of, University, Ala.	4	W. J. Miller	Frank Tompkins	Louise Inge
Alberta, Univ. of, Edmonton, Alberta, Can.	10	R. E. Phillips	B. McDiarmaid	D. MacDonald
Arizona, Univ. of, Tucson, Ariz.	8	J. C. Clark	S. H. Foote	Herbert Jacobs
Arkansas, Univ. of, Fayetteville, Ark.	7	W. B. Stelzner	W. R. Thomas	D. P. Patterson
British Columbia, Univ. of, Vancouver, Can.	10	W. B. Coulthard	S. J. Beaton	O. M. Julson
Brooklyn, Poly. Inst. of, Brooklyn, N. Y.	3	A. B. Giordano		
(Day Division)			Eugene Lovette	Wm. Stolze
(Evening Division)			A. E. Ruppel	
Brown Univ., Providence, R. I.	1	F. N. Tompkins	W. H. Kimball	D. C. Taylor
Bucknell Univ., Lewisburg, Pa.	2	K. B. MacKichan	Henry Parkin	John Raudenbush
Calif. Inst. of Tech., Pasadena, Calif.	8	F. W. Maxstadt	R. Lauterbach	Alfred Robinson
Calif., Univ. of, Berkeley, Calif.	8	P. L. Morton	Henry Doeleman	J. B. Duryea
Carnegie Inst. of Tech., Pittsburgh, Pa.	2	G. R. Patterson	W. W. Carpenter	E. A. Sagan
Case School of Applied Science, Cleveland, Ohio	2	P. L. Hoover	R. E. Rohrer	G. B. Ober
Catholic Univ. of America, Washington, D. C.	2	T. J. MacKavanagh	Wm. Whelan	P. J. Bayer
Cincinnati, Univ. of, Cincinnati, Ohio	2	L. R. Culver	W. J. Gruber	C. G. Haehnle
Clarkson College of Tech., Potsdam, N. Y.	1	A. R. Powers	R. J. Whalen	G. E. McElroy
Clemson Agri. College, Clemson, S. C.	4	F. T. Tingley	E. Hutto, Jr.	J. E. Webb
Colorado State Col. of A. & M. Arts, Fort Collins, Colo.	6	F. B. Beatty	Glen Powers	Ralph Green
Colorado, Univ. of, Boulder, Colo.	6	H. B. Palmer	Douglas Trego	R. E. McCosh
Columbia Univ., New York, N. Y.	3	J. R. Ragazzini	Edward Buyer	C. P. Thomas
Connecticut, Univ. of, Storrs, Conn.	1	G. S. Timoshenko	J. L. Stern	R. J. Jeffries
Cooper Union, New York, N. Y.	3	J. W. Hostetter		
(Day Division)			Seymour Levander	Thomas Schwartz
(Evening Division)			J. J. Coughlin	S. Meyers
Cornell Univ., Ithaca, N. Y.	1	E. T. B. Gross	Robert Garmezy	Richard Koch
Delaware, Univ. of, Newark, Del.	2	M. G. Young	Wm. Schuster	Clarence Perry
Denver, Univ. of, Denver, Colo.	6	F. H. McClain	Maynard Thompson	Walter Magnuson
Detroit, Univ. of, Detroit, Mich.	5	H. O. Warner	J. P. Bellafaire	F. W. Rowe
Drexel Inst. of Tech., Philadelphia, Pa.	2	E. O. Lange	Paul Beroza	D. E. Thomas
Duke Univ., Durham, N. C.	4	O. J. Meier, Jr.	K. E. Sanger	Charles Myers
Florida, Univ. of, Gainesville, Fla.	4	E. F. Smith	Leslie Stroup	O. R. Gano
George Washington Univ., Washington, D. C.	2		Ned Schreiner	George Conrad
Georgia School of Tech., Atlanta, Ga.	4	H. B. Duling	R. E. Morris	L. S. Apple
Harvard Univ., Cambridge, Mass.	1	J. D. Cobine	J. C. Fisher	
Idaho, Univ. of, Moscow, Idaho	9	J. Hugo Johnson	R. F. Conrad	Howard Kambitsch
Illinois Inst. of Tech., Chicago, Ill.	5	E. H. Freeman	James McDonald	Irving Kaluzna
Illinois, Univ. of, Urbana, Ill.	5	E. A. Reid	V. A. Rydbeck	J. E. Hinchcliffe
Iowa State College, Ames, Iowa	5	B. S. Willis	John Lagerstrom	
Iowa, Univ. of, Iowa City, Iowa	5	H. R. Reed	E. E. Carlson	V. W. Chabal
Johns Hopkins Univ., Baltimore, Md.	2	F. Hamburger, Jr.	Vernon Evans	Earl Keller
Kansas State College, Manhattan, Kan.	7	J. E. Ward, Jr.	R. J. Cossaart	A. K. Kingsley
Kansas, Univ. of, Lawrence, Kan.	7	G. A. Richardson	Frank Blue	Glen Sankey
Kentucky, Univ. of, Lexington, Ky.	4	Brinkley Barnett	R. L. Hucaby	
Lafayette College, Easton, Pa.	2	F. W. Smith	Paul Ackroyd	G. P. Clute, Jr.
Lehigh Univ., Bethlehem, Pa.	2	C. G. Brennecke	Gordon Roberts	Ralph Lau
Louisiana State Univ., Baton Rouge, La.	4	A. K. Ramsey	A. B. Daniels	L. E. Gulledge
Louisville, Univ. of, Louisville, Ky.	4	M. G. Northrop	L. E. Vordenbrueggen	J. L. Wedekind
Maine, Univ. of, Orono, Me.	1	N. E. Wilson	D. E. Davis	
Manhattan College, New York, N. Y.	3	R. T. Weil	R. E. Blewitt	D. G. O'Connor
Marquette Univ., Milwaukee, Wis.	5	J. F. H. Douglas	Arthur Zimmer	D. Bernhagen
Maryland, Univ. of, College Park, Md.	2	L. J. Hodgins	Lyal Merriken	Wade Dorsett
Mass. Inst. of Tech., Cambridge, Mass.	1	K. L. Wildes	J. N. Childs	J. B. Angell
Michigan Col. of Min. & Tech., Houghton, Mich.	5	C. Russell	R.E. Sherman	W. T. Ames
Michigan State Col., E. Lansing, Mich.	5	M. M. Cory	W. P. Bennett	J. E. Allen
Michigan, Univ. of, Ann Arbor, Mich.	5	J. S. Gault	M. A. Gilleo	R. E. Miller
Milwaukee Sch. of Engg., Milwaukee, Wis.	5	E. L. Wiedner	Gordon Stanhope	D. C. Schmidt
Minnesota, Univ. of, Minneapolis, Minn.	5	J. H. Kuhlmann	W. F. Carter	E. J. Proszek
Mississippi State Col., State College, Miss.	4	N. M. McCorkle	F. E. Uithoven	J. E. King, Jr.
Missouri Sch. of Mines & Met., Rolla, Mo.	7	J. S. Johnson	G. E. Barber	R. E. Gogan
Missouri, Univ. of, Columbia, Mo.	7	D. Waidelich	B. J. Gastineau	Robert Roney
Montana State College, Bozeman, Mont.	9	E. W. Schilling	Howard Ellsworth	Rod Auclair
Nebraska, Univ. of, Lincoln, Neb.	6	O. E. Edison	C. A. Stutt	J. V. Jensen
Nevada, Univ. of, Reno, Nevada	8	S. G. Palmer	Wm. Richter	Robert Howard
Newark Col. of Engg., Newark, N. J.	3	F. A. Russell	R. H. Rose	J. G. Friedman
New Hampshire, Univ. of, Durham, N. H.	1	W. B. Nulsen	Richard Foley	A. G. Yeaton
New Mexico State Col., State College, N. M.	7	M. A. Thomas	Jack Burleson	Paul Russell
New Mexico, Univ. of, Albuquerque, N. M.	7	R. W. Tapy	Edward Ancona	N. Maguire
New York, Col. of the City of, New York	3	Harry Baum		
(Day Division)			N. Rynkowsky	Irving Flyer
(Evening Division)				
New York Univ., New York	3	P. C. Cromwell		
(Day Division)			Thomas Fentress	Sidney Hasin
(Evening Division)			N. M. Vrana	C R. Morrell
North Carolina State Col., Raleigh, N. C.	4	L. M. Keever	G. B. Stevens	P. D. Strum
North Dakota State Col., Fargo, N. D.	5	H. S. Rush	Waldron Wigtil	Marvin Gebhardt
North Dakota, Univ. of, Grand Forks, N. D.	5	C. W. Rook	Ernest Lees	Herbert Frey
Northeastern Univ., Boston, Mass.	1	R. G. Porter		
(Division A)			T. F. Mahoney	J. O'Leary
(Division B)			G. P. Minalga	J. E. Mrose
Northwestern Univ., Evanston, Ill.	5	R. W. Jones	Chad Pierce	G. Gauer
Norwich Univ., Northfield, Vt.	1	D. E. Howes	J. S. Allen	W. Flint
Notre Dame, Univ. of, Notre Dame, Ind.	5	J. A. Northcott	G. A. Charters	R. J. Martina
Ohio Northern Univ., Ada, Ohio	2	G. W. Klein	Ralph Walters	H. A. Ault
Ohio State Univ., Columbis, Ohio	2	S. O. Evans	Edward Hayden	John Birch
Ohio Univ., Athens, Ohio	2		Thomas Raymond	
Oklahoma A. & M. Col., Stillwater, Okla.	7	A. Naeter	G. L. Lucky	Stephen Nelson
Oklahoma, Univ. of, Norman, Okla.	7	R. A. Church	F. S. Chaffin	J. C. Curry
Oregon State Col., Corvallis, Ore.	9	W. H. Huggins	C. W. Eastman	C. E. Allen
Pennsylvania State Col., State College, Pa.	2	P. X. Rice	E. F. Von Arx	E. P. Diehl

Student Branches of the Institute (continued)

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Pittsburgh, Univ. of, Pittsburgh, Pa.	2	R. C. Gorham	K. E. Doriot	A. J. Baeslack
Porto Rico, Univ. of, Mayaguez, P. R.	3	M. Wiewall, Jr.	R. A. Preston	J. E. Gomez
Pratt Institute, Brooklyn, N. Y.	3	D. H. Wright	Martin Lewis	M. H. Kitchell
Princeton Univ., Princeton, N. J.	2	W. C. Johnson	R. G. Mills	G. R. Compton III
Purdue Univ., Lafayette, Ind.	5	J. H. Karr	R. E. Williams	W. M. Robinson
Rensselaer Poly. Inst., Troy, N. Y.	1	E. D. Broadwell	P. E. Smith	John Litsios
Rhode Island State Col., Kingston, R. I.	1	W. J. Mowbray	Donald Campbell	Paul Ross
Rice Institute, Houston, Texas	7	C. R. Wischmeyer	Hugh Saye	J. H. Valcik
Rose Poly. Inst., Terre Haute, Ind.	5	C. C. Knipmeyer	R. J. Howell	R. D. Calvert
Rutgers Univ., New Brunswick, N. J.	3	J. L. Potter	M. Taubenslag	Wm. Blauvelt
Santa Clara, Univ. of, Santa Clara, Calif.	8	W. J. Warren	R. P. Rossi	E. M. Selle
South Carolina, Univ. of, Columbia, S. C.	4	W. M. Bauer	J. D. Carson	Enoch Smith
South Dakota State Col., Brookings, S. D.	5	Wm. H. Gamble	B. O. Randall	V. V. Nielson
So. Dakota State School of Mines & Tech., Rapid City, S. D.	6	E. E. Clark	Donald Sullivan	W. H. Gardner
Southern California, Univ. of, Los Angeles, Calif.	8	P. S. Biegler	Howard Helfman	Alois Jokl
Southern Methodist Univ., Dallas, Texas	7	E. H. Flath	R. R. Hair	John Savage
Stanford Univ., Stanford University, Calif.	8	H. H. Skilling	P. M. Keeler	W. H. Levers
Stevens Inst. of Tech., Hoboken, N. J.	3	Wm. L. Sullivan		
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Syracuse Univ., Syracuse, N. Y.	1	C. W. Henderson	N. B. Levey	P. J. Harter
Tennessee, Univ. of, Knoxville, Tenn.	4	W. O. Leffell	Robert Powell	T. A. Pomeroy
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Texas Technological Col., Lubbock, Texas	7	C. V. Bullen	O. L. Cheaney	Hal Pender
Texas, Univ. of, Austin, Texas	7	R. A. Galbraith	Harold Smith	Jay Seefeld
Tufts Col., Tufts College, Mass.	1	A. H. Howell	John Meadows	Walter Huening
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Union Col., Schenectady, N. Y.	1	H. W. Bibber	John Schamberger	John Mann
Utah, Univ. of, Salt Lake City, Utah	9	L. D. Harris	C. F. Quate	R. W. Johnson
Vanderbilt University, Nashville, Tenn.	4	S. R. Schealer	H. M. Bailey	F. T. Ragan, Jr.
Vermont, Univ. of, Burlington, Vt.	1	E. R. McKee	D. L. Clark	R. F. O'Grady
Villanova, Col., Villanova, Pa.	2	H. S. Bueche	Anthony Mannino	J. L. O'Brien
Virginia Military Inst., Lexington, Va.	4	J. S. Jamison	V. J. Thomas	F. A. Collins
Virginia Poly. Inst., Blacksburg, Va.	4	Claudius Lee	C. E. Carr	R. B. Fetter
Virginia, Univ. of, University, Va.	4	J. S. Miller, Jr.	R. A. Dudley	P. B. Peyton, Jr.
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Washington, Univ. of, Seattle, Wash.	9	R. E. Lindblom	A. R. True	Lloyd Elliott
Washington Univ., St. Louis, Mo.	7	D. A. Fischer	E. W. Wechsler	C. W. Jeep, Jr.
West Virginia Univ., Morgantown, W. Va.	2	A. H. Forman	G. M. Powers	D. R. Steele
Wisconsin, Univ. of, Madison, Wis.	5	J. R. Price	J. J. Lyons	M. W. Oleson
Worcester Poly. Inst., Worcester, Mass.	1	V. Siegfried	F. J. Mickiewicz	E. G. Baldwin
Wyoming, Univ. of, Laramie, Wyo.	6	G. H. Sechrist	E. R. Allen	Y. Hattori
Yale Univ., New Haven, Conn.	1	A. G. Conrad	A. B. Williams	T. H. Hedene

Total Branches..........125

Number of Enrolled Students November 1, 1943—3,573

Geographical District Executive Committees

District	Chairman (Vice-President, AIEE)	Secretary (District Secretary)	Chairman, District Committee on Student Activities
1 North Eastern	K. B. McEachron, General Electric Company, 100 Woodlawn Ave., Pittsfield, Mass.	Victor Siegfried, Worcester Polytechnic Institute, Worcester, Mass	R. G. Porter, Northeastern University, Boston, Mass
2 Middle Eastern	W. E. Wickenden, Case School of Applied Science, Cleveland 6, Ohio	H. R. Vaughan, Westinghouse Elec. & Mfg. Co., East Pittsburgh, Pa.	P. X. Rice, Pennsylvania State College, State College, Pa.
*3 New York City	C. R. Jones, Westinghouse Elec. & Mfg. Co., 40 Wall St., New York 5, N. Y.	R. L. Webb, Consolidated Edison Co. of New York, Inc., 4 Irving Place, New York 3, N. Y.	D. H. Wright, Pratt Institute, Brooklyn, N. Y.
4 Southern	C. W. Ricker, Tulane University, New Orleans 15, La.	F. E. Johnson, Jr., 317 Baronne St., New Orleans 9, La.	Claudius Lee, Virginia Polytechnic Institute, Blacksburg, Va.
5 Great Lakes	A. G. Dewars, Northern States Power Co., 15 S. 5th St., Minneapolis, Minn.	N. C. Pearcy, Public Utility Engg. & Service Corp., 231 S. La Salle St., Chicago 4, Ill.	M. M. Cory, Michigan State College, East Lansing, Mich.
6 North Central	L. A. Bingham, University of Nebraska, Lincoln, Nebr.	Frank C. Howard, Iowa-Nebraska Light & Power Co., Lincoln, Nebr.	H. B. Palmer, University of Colorado, Boulder, Colo.
7 South West	E. T. Mahood, Southwestern Bell Telephone Co., 11th & Oak Sts., Kansas City, Mo.	R. G. Kloeffler, Kansas State College, Manhattan, Kans.	M. A. Thomas, New Mexico State College, State College, N. M.
8 Pacific	J. M. Gaylord, Metropolitan Water District of Southern California, 306 W. 3rd St., Los Angeles 13, Calif.	Mark A. Sawyer, Box 5300, Metropolitan Station, Los Angeles, Calif.	F. W. Maxstadt, California Institute of Technology, Pasadena 4, Calif.
9 North West	E. W. Schilling, Montana State College, Bozeman, Mont.	W. A. Boyer, Anaconda Copper Mining Co., Butte, Mont.	L. D. Harris, University of Utah, Salt Lake City, Utah
10 Canada	W. J. Gilson, 1244 Dufferin St., Toronto, Ont.	W. Roy Harmer, H.E.P.C. of Ontario, 620 University Ave., Toronto. Ont.	

* District 3 includes all foreign countries except Canada.

Note: Each District executive committee includes also the chairmen and secretaries of all Sections within the District, and the District vice-chairman of the national membership committee.

Sections of the Institute

Name	District	When Organized	Membership Aug. 1, 1943	Chairman	Secretary	Secretary's Address
Akron	2	Aug. 12, '20	87	R. F. Snyder	H. L. Smith	Goodyear Tire & Rubber Co., Akron, Ohio
Alabama	4	May 22, '29	42	E. R. Coulbourn	C. F. Sittloh	Alabama Power Co., Birmingham, Ala.
Arizona	8	Mar. 22, '41	37	Malcolm Bridgwater	E. A. Gissel	Central Arizona Light & Power Co., Phoenix, Ariz.
Boston	1	Feb. 13, '03	574	T. Cooper, Jr.	W. I. Middleton	Simplex Wire & Cable Co., Cambridge, Mass.
Central Indiana	5	Jan. 12, '12	133	C. R. Swenson	C. E. Parks	Public Service Co. of Indiana, Inc., Indianapolis, Ind.
Chicago	5	1893	849	F. E. Keith	R. C. Ericson	Northern Indiana Public Service Co., Hammond, Ind.
Cincinnati	2	June 30, '20	151	V. G. Rettig	W. H. Breunig	303 West Third St., Cincinnati 2, Ohio
Cleveland	2	Sept. 27, '07	368	P. L. Hoover	V. A. Diggs	Ohio Bell Telephone Co., 750 Huron Rd., Cleveland, Ohio
Columbus	2	Mar. 17, '22	82	S. O. Evans	R. W. Miner	323 No. Ardmore Road,, Columbus 9, Ohio
Connecticut	1	Apr. 16, '21	365	E. C. Brown	C. D. Hewitt	P.O. Box 1562, New Haven 6, Conn.
Dayton	2	June 9, '43	122	L. J. Fritz	J. W. Gehrke	Dayton Power & Light Co., 25 No. Main St., Dayton 2, Ohio
Denver	6	May 18, '15	186	W. H. Taylor	L. R. Patterson	Public Service Co. of Colorado, Denver, Colo.
East Tennessee	4	Sept. 2, '36	134	R. M. Ferrill	W. R. Gilkeson	Tennessee Valley Authority, Chattanooga, Tenn.
Erie	2	Jan. 11, '18	71	H. N. Shaw	W. D. Bearce	4121 Sassafras St., Erie, Pa.
Florida	4	Jan. 28, '31	136	C. F. Titus	P. J. Carlin	Florida Power & Light Co., Miami, Fla.
Fort Wayne	5	Aug. 14, '08	100	C. W. Kronmiller	E. G. Downie	General Electric Co., Fort Wayne, Ind.
Georgia	4	Jan. 14, '04	101	J. E. Mellett	Carl Evans	Electrical South, 1020 Grant Bldg., Atlanta 3, Ga.
Houston	7	Aug. 7, '28	153	H. P. Heafer	Hezzie Clark	P.O. Drawer 2220, Houston 1, Tex.
Iowa	5	June 25, '29	80	W. B. Boast	E. B. Fowler	Northwestern Bell Telephone Co., Des Moines 9, Iowa
Ithaca	1	Oct. 15, '02	60	A. B. Credle	W. E. Meserve	Cornell University, Ithaca, N. Y.
Kansas City	7	Apr. 14, '16	143	V. P. Hessler	S. H. Pollock	Box 679, Kansas City 10, Mo.
Lehigh Valley	2	Apr. 16, '21	189	R. M. Wyatt	L. Z. Ludorf	Penn. Pwr. & Lt. Co., 135 N. Washington St., Wilkes Barre, Pa.
Los Angeles	8	May 19, '08	548	L. F. Hunt	E. S. Condon	357 South Hill St., Los Angeles 13, Calif.
Louisville	4	Oct. 15, '26	75	L. G. Weiser	M. S. Winstandley	Louisville Gas & Elec. Co., Louisville, Ky.
Lynn	1	Aug. 22, '11	202	W. R. Cox	C. A. Atherton	Champion Lamp Works, 600 Broad St., Lynn, Mass.
Madison	5	Jan. 8, '09	75	F. D. Mackie	E. H. Scheibe	144 W. Gilman St., Madison 3, Wis.
Mansfield	2	Mar. 6, '39	68	R. Felver	M. A. Giles	589 Crescent Road, Mansfield, Ohio
Maryland	2	Dec. 16, '04	349	M. W. Pullen	J. L. Hildebrandt	Consol. Gas Elec. Lt. & Pwr. Co., Monument St. Bldg., Baltimore 3, Md.
Memphis	4	May 22, '30	57	J. F. Fossick	M. G. Sifford	Westinghouse Electric Supply Co., Memphis 1, Tenn.
Mexico	3	June 29, '22	57	F. A. Nava	F. Aubert	Apartado Postal 1399, Mexico, D. F. Mexico
Michigan	5	Jan. 13, '11	393	J. W. Bishop	M. M. Cory	Michigan State College, East Lansing, Mich.
Midwestern Canada	10	Oct. 14, '25	26	M. L. Haynes	J. R. Young	1945 Scarth St., Regina, Sask., Can.
Milwaukee	5	Feb. 11, '10	318	R. H. Earle	E. L. McClure	Wisconsin Elec. Pwr. Co., 231 W. Michigan St., Milwaukee 3, Wis.
Minnesota	5	Apr. 7, '02	118	W. H. Gille	L. A. Griffith	Minneapolis-Honeywell Regulator Co., Minneapolis, Minn.
Montana	9	June 24, '31	40	C. R. Davis	W. H. Blankmeyer	Montana Power Co., Butte, Mont.
Montreal	10	Apr. 16, '43	149	Frederick Krug	D. M. Farnham	107 Craig St. West, Room 307, Montreal, Que., Can.
Muscle Shoals	4	Feb. 18, '38	23	Leo King	Arthur Vaughn	702 Commons, Tuscumbia, Ala.
Nebraska	6	Jan. 21, '25	50	C. P. Kahler	I. M. Ellestad	Northwestern Bell Telephone Co., Omaha, Nebr.
New Mexico–West Texas	7	Mar. 7, '40	48	E. C. Wise	G. V. Cooper	El Paso Electric Co., El Paso, Texas
New Orleans	4	Dec. 8, '33	141	C. P. Knost	F. E. Johnson	317 Baronne St., New Orleans 9, La.
New York	3	Dec. 10, '19	3,586	C. S. Purnell	J. L. Callahan	R.C.A. Communications, Inc., 66 Broad St., New York 4, N. Y.
Niagara Frontier	1	Feb. 10, '25	186	C. E. Gaylord	W. J. Freudenberger	Buffalo Niagara Electric Corp., Buffalo 3, N. Y.
North Carolina	4	Mar. 21, '29	99	H. B. Robinson	L. M. Keever	North Carolina State College, Raleigh, N. C.
North Texas	7	May 18, '28	164	H. G. Mathewson	E. H. Flath	Southern Methodist University, Dallas, Tex.
Oklahoma City	7	Feb. 16, '22	90	R. W. Linney	C. L. Jobe	Oklahoma Gas & Electric Co., Oklahoma City 2, Okla.
Philadelphia	2	Feb. 18, '03	863	H. E. Strang	M. L. Lehman	American Tel. & Tel. Co., 530 Bourse Bldg., Philadelphia 6, Pa.
Pittsburgh	2	Oct. 13, '02	657	G. W. Penney	R. C. Gorham	University of Pittsburgh, Pittsburgh, Pa.
Pittsfield	1	Mar. 25, '04	195	J. R. Meador	D. D. MacCarthy	General Electric Co., Pittsfield, Mass.
Portland	9	May 18, '09	177	A. O. Mangold	W. E. Enns	Portland General Elec. Co., Portland, Oreg.
Providence	1	Mar. 12, '20	93	H. P. Turner	G. E. Andrews	40 Bridgham St., Providence, R. I.
Rochester	1	Oct. 9, '14	121	Walter Criley	O. L. Angevine, Jr.	Stromberg-Carlson Co., 100 Carlson Rd., Rochester, 3, N. Y.
St. Louis	7	Jan. 14, '03	295	F. A. Cooper	F. J. McCluskey	James R. Kearney Corp., 4236 Clayton Ave., St. Louis, Mo.
San Diego	8	Jan. 18, '39	56	W. T. Johnson	J. W. Doolittle	Southern California Tel. Co., 914 C St., San Diego 1, Calif.
San Francisco	8	Dec. 23, '04	601	C. E. Baugh	D. I. Anzini	General Electric Co., 804 Russ Bldg., San Francisco 4, Calif.
Schenectady	1	Jan. 26, '03	630	O. C. Rutledge	P. H. Light	General Electric Co., Schenectady, N. Y.
Seattle	9	Jan. 19, '04	249	H. V. Strandberg	C. D. Howe	Pacific Tel. & Tel. Co., Seattle, Wash.
Sharon	2	Dec. 11, '25	162	H. S. Gates	F. D. Fielder	1324 Yahres Road, Sharon, Pa.
South Bend	5	Feb. 26, '41	48	L. F. Stauder	C. M. Dunn	General Electric Co., 112 W. Jefferson, South Bend, Ind.
South Carolina	4	Mar. 2, '40	55	W. H. Kendrick	F. T. Tingley	Clemson College, Clemson, S. C.
South Texas	7	May 23, '30	50	G. E. Schmitt	S. R. Friedsam	Lower Colorado River Authority, Austin, Tex.
Spokane	9	Feb. 14, '13	85	H. E. Mellrud	J. F. Gogins	General Electric Co., 421 W. Riverside Ave., Spokane 8, Wash.
Springfield	1	June 29, '22	51	A. L. Davis	B. N. Durfee	28 Talcott Ave., West Springfield, Mass.
Syracuse	1	Aug. 12, '20	129	M. H. Pratt	J. W. Dice	Westinghouse Elec. & Mfg. Co., 420 N. Geddes St., Syracuse 4, N. Y.
Toledo	2	June 3, '07	88	E. B. Thurston	W. M. Campbell	2145 Central Grove Ave., Toledo, Ohio
Toronto	10	Sept. 30, '03	381	F. C. Barnes	T. C. D. Churchill	76 Adelaide St., West, Toronto, Ont., Can.
Tulsa	7	Oct. 1, '37	79	W. E. Slemmer	P. E. Gentry	Southwestern Bell Tel. Co., 424 So. Detroit Ave., Tulsa 3, Okla.
Urbana	5	Nov. 25, '02	77	G. R. Peirce	A. D. Bailey	University of Illinois, Urbana, Ill.
Utah	9	Mar. 9, '17	85	T. A. Robinson	J. A. McDonald	General Electric Co., P.O. Box 779, Salt Lake City 9, Utah
Vancouver	10	Aug. 22, '11	98	T. Ingledow	L. B. Stacey	570 Dunsmuir St., Vancouver, B. C., Can.
Virginia	4	May 19, '22	147	C. E. Creecy	A. F. Forbes	Newport News Shipbuilding & Drydock Co., Newport News, Va.
Washington	2	Apr. 9, '03	675	F. B. Silsbee	W. F. Dietz	Westinghouse Elec. & Mfg. Co., 1625 K St., N. W., Washington, D. C.
West Virginia	2	Apr. 9, '40	38	A. M. Rosenblatt	P. M. Barlow	Ravens Park, St. Albans, W. Va.
Wichita	7	Sept. 16, '37	62	G. W. Fisher	R. F. Dice	Kansas Gas & Elec. Co., 201 N. Market St., Wichita 1, Kans.
Worcester	1	Feb. 18, '20	57	J. H. Jewell	R. M. Peirce	American Steel & Wire Co., Electrical Cable Works, Worcester, Mass.

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Niagara Falls (Niagara Frontier Section)	C. G. Moore	A. W. Walmsley	Electro Metallurgical Co., Niagara Falls, N. Y.
Rock River Valley (Madison Section)	C. V. Bullen	B. T. Anderson	625 Rome Ave., Rockford, Ill.
Zanesville (Columbus Section)	J. O. Fenwick	J. G. Everhart	Line Material Co., Zanesville, Ohio

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